VOLUME 105 PART A NUMBER 21 JUNE 1958

The Proceedings

OF

THE INSTITUTION OF ELECTRICAL ENGINEERS

FOUNDED 1871: INCORPORATED BY ROYAL CHARTER 1921

PART A

POWER ENGINEERING

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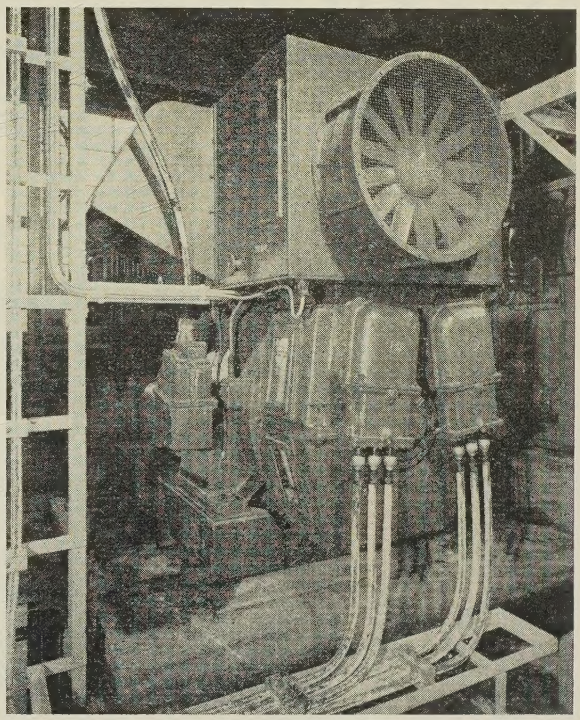
The closed air-circuit construction may be applied to practically all machines in the L.S.E. range. It is particularly useful in the case of N-S variable-speed a.c. motors, which normally require an independent cooling fan when the speed range is wide (due to the low efficiency of an integral fan at low speeds).



N-S variable-speed a.c. motor, 250/108 h.p., 960/400 r.p.m. closed air-circuit with air/air heat exchanger. Independent motor drives fans for both internal and external air circulation. For CO_2 compressor drive (Associated Ethyl Ltd.)

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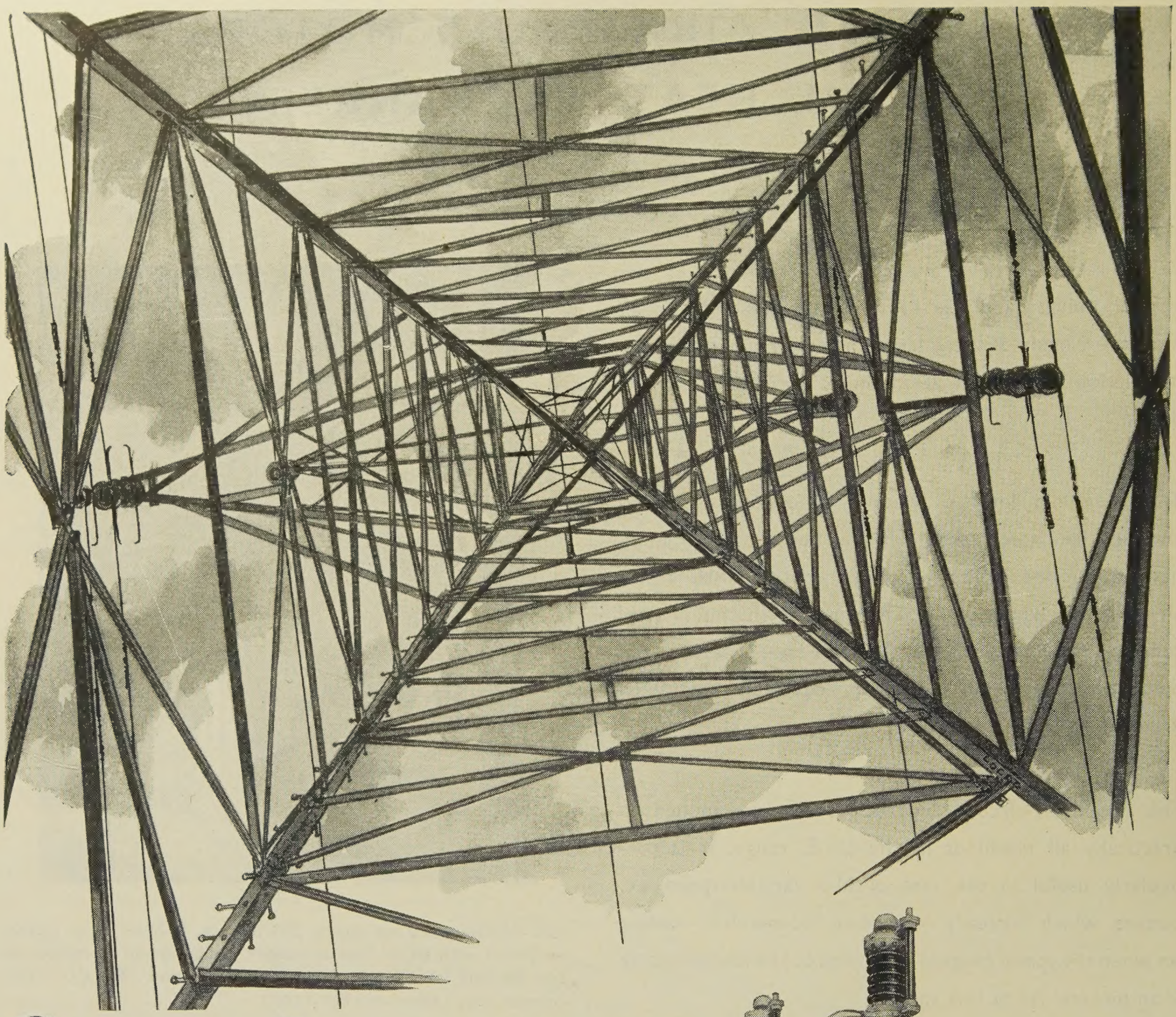
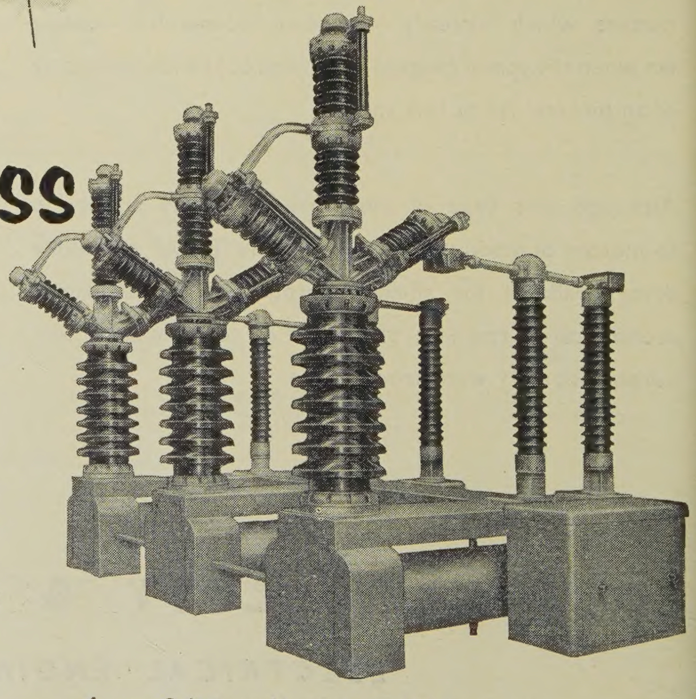


Pattern for progress

The development of the power supply system, including the 275kV Supergrid extension inaugurated by the Central Electricity Generating Board created an increased need for "Ferguson Pailin" Switchgear of many types, for both indoor and outdoor service.

Among these are the heavy-duty air-blast circuit-breakers, type CA10 now being installed in Carmarthen Bay Power Station—the first "hall-type" station to be commissioned in Gt. Britain. They form an extension to the original installation of type CA9 air-blast circuit-breakers, also of "Ferguson Pailin" manufacture.

The type CA10 circuit-breakers have a rating of 3500 MVA at 132kV.



A type CA10 air-blast circuit-breaker

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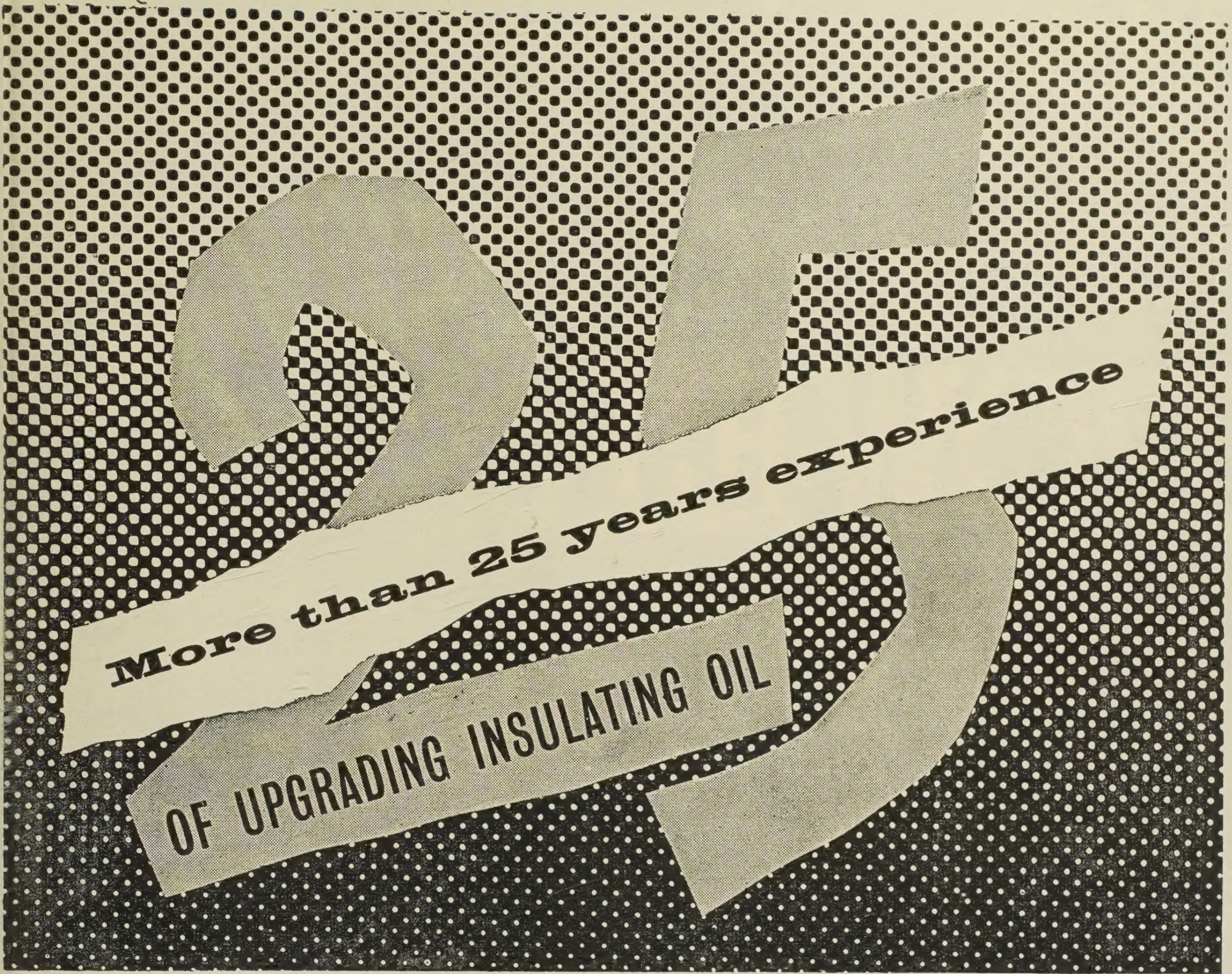

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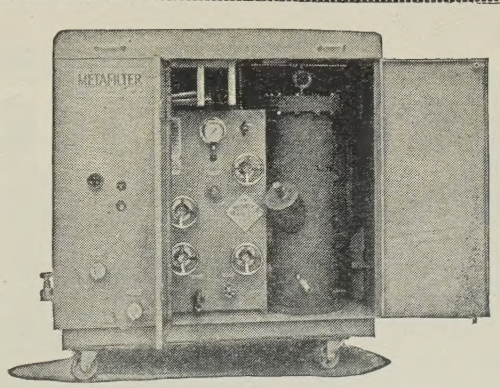

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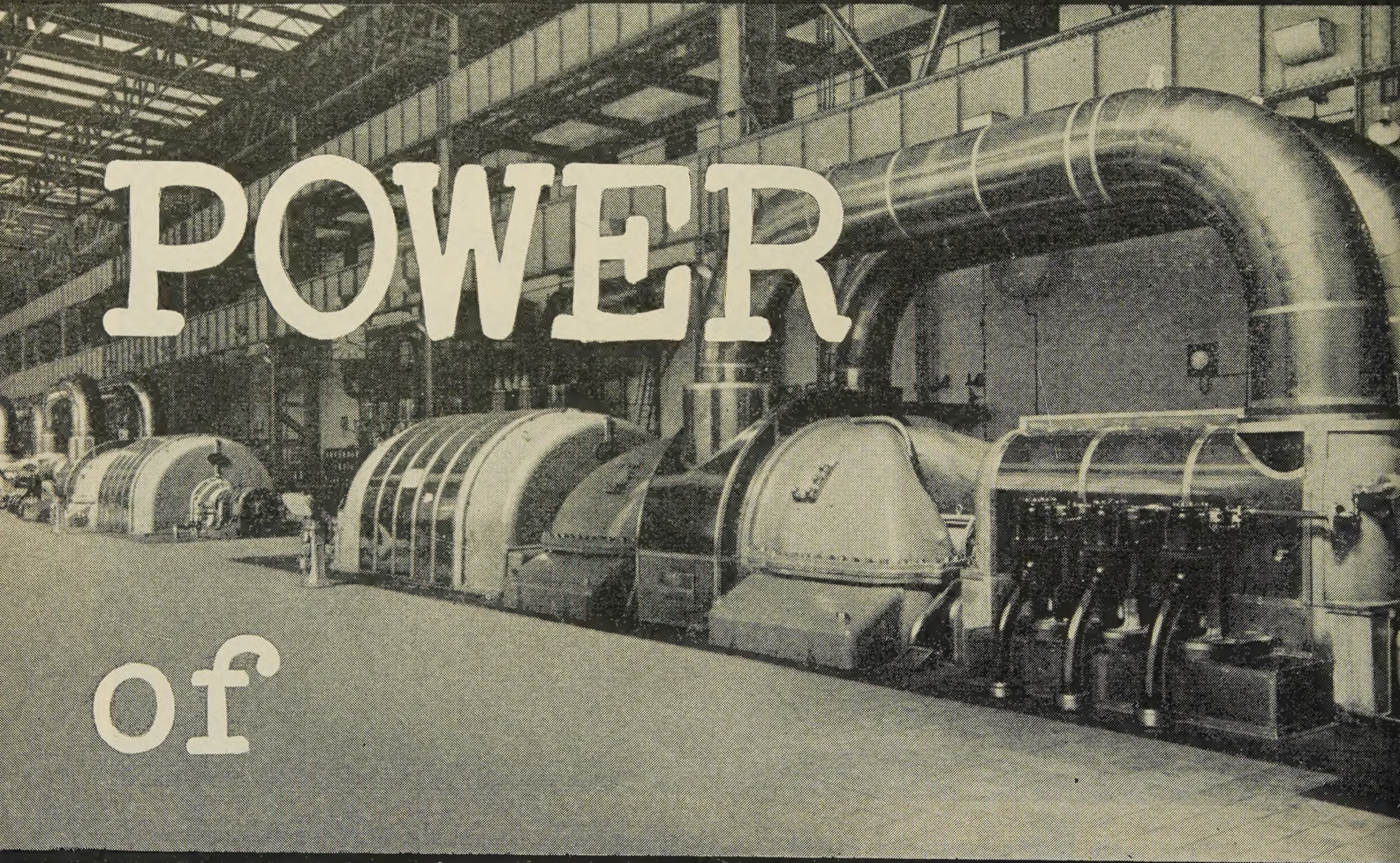

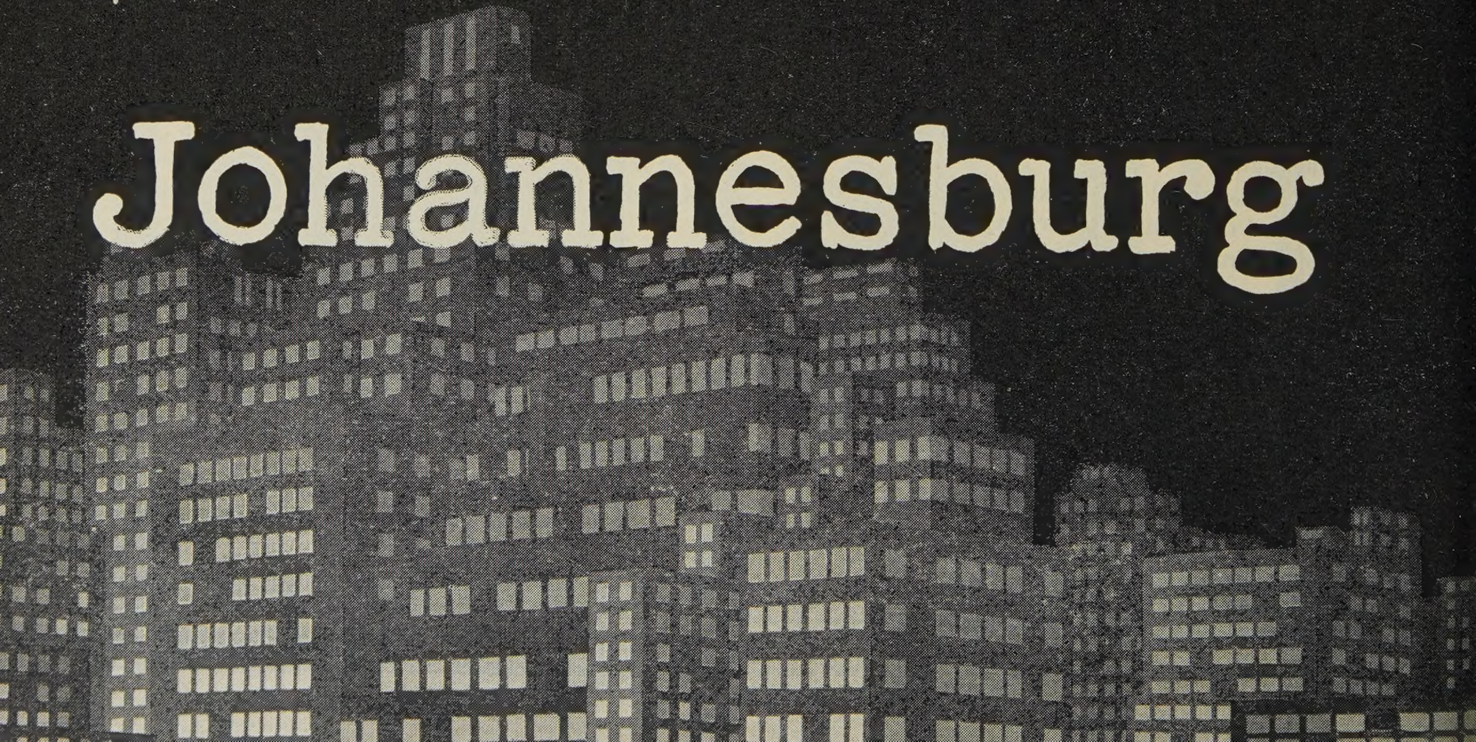

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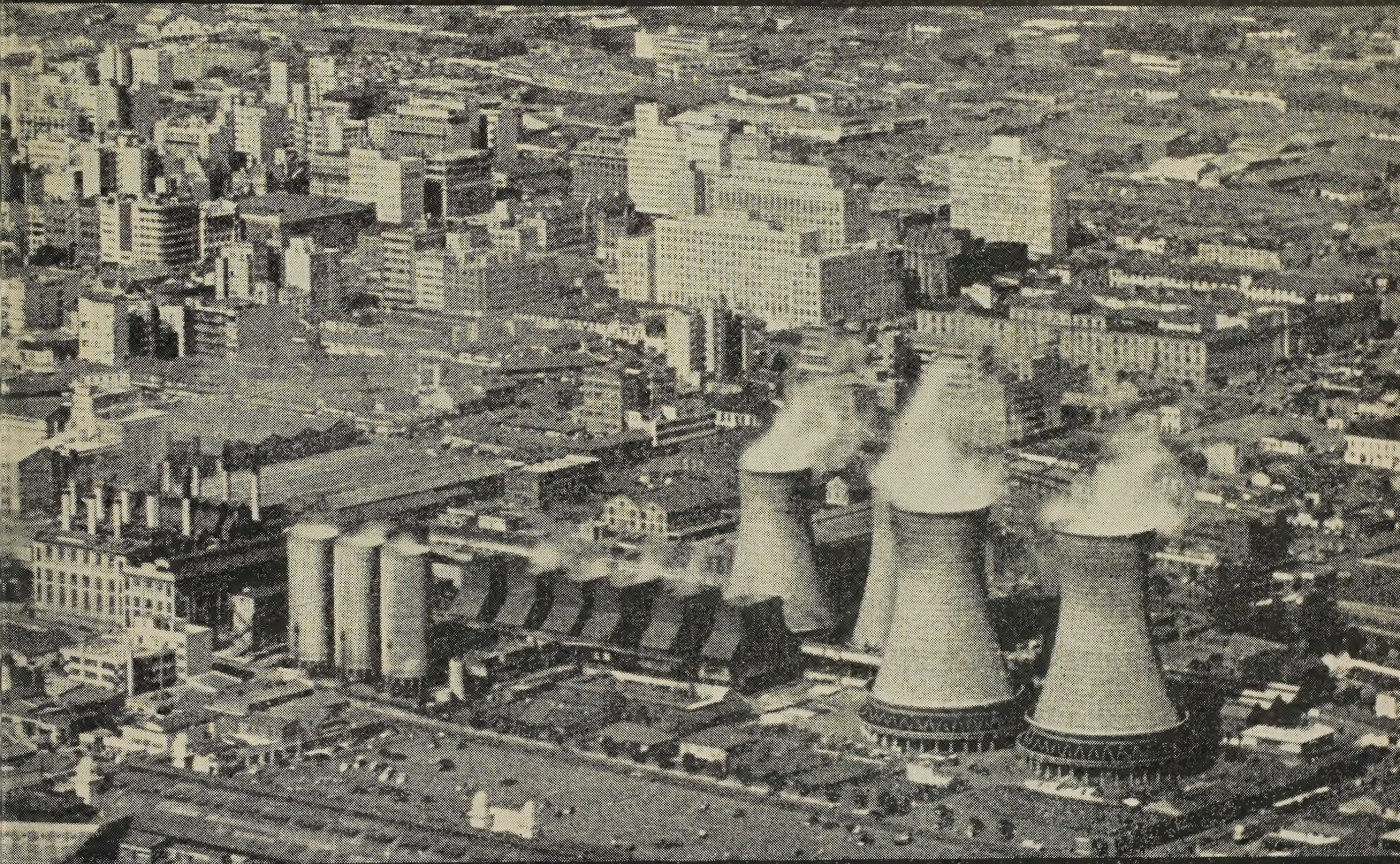

With the latest contract for two 60 MW turbo-generators, the number of G.E.C. sets in Johannesburg's four power stations will total twenty five and will provide three quarters of a million kilowatts. And so the G.E.C. serves Johannesburg with the power to keep a great city alive. A big job indeed—and just the job for the G.E.C. with its vast experience of electrical generation, distribution and control.

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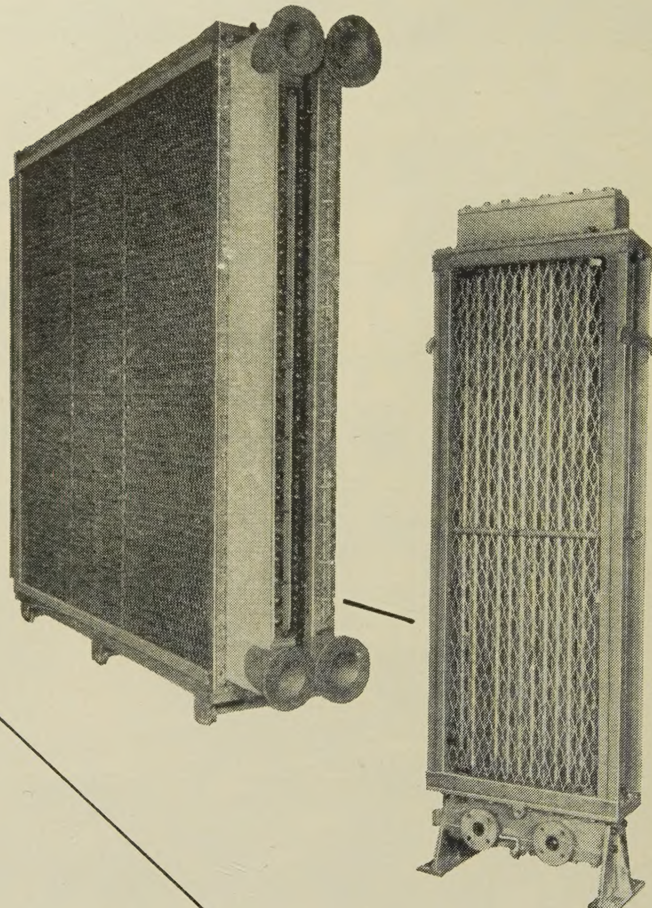

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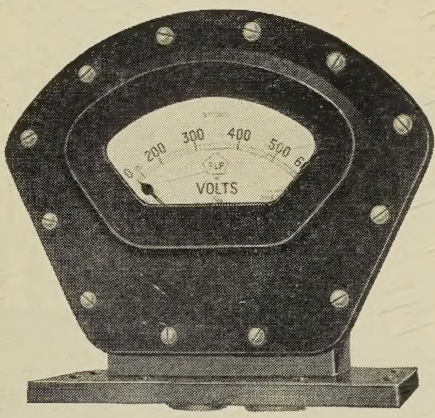
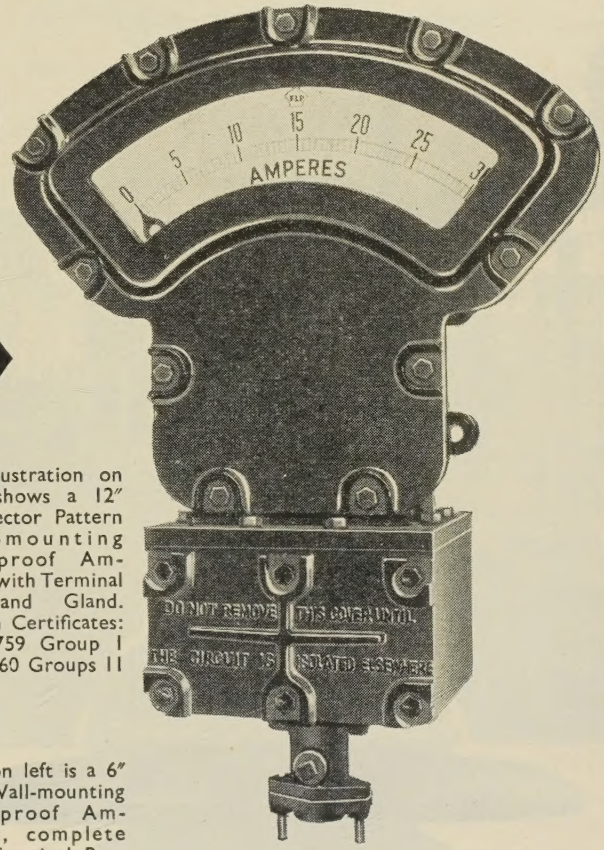
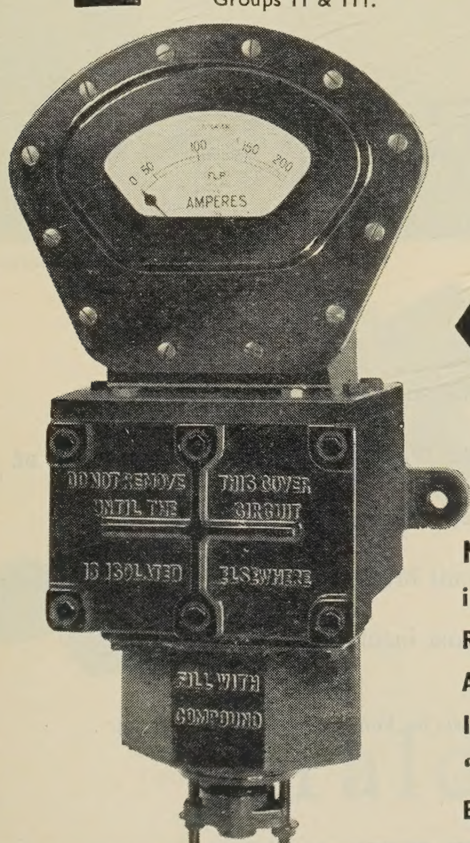


Illustrated above is a Controller type Flameproof Instrument, with a 6″ dial. Buxton Certificates: FLP 2097 Group I FLP 2098 Groups II & III.

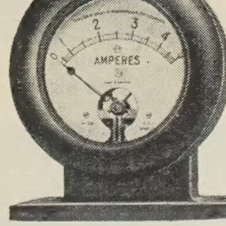


The illustration on right shows a 12″ dial, Sector Pattern Wall-mounting Flameproof Ammeter with Terminal Box and Gland. Buxton Certificates: FLP 3759 Group I FLP 3760 Groups II & III.



Seen on left is a 6″ dial, Wall-mounting Flameproof Ammeter, complete with Terminal Box, Compound Sealing Chamber and Gland. Buxton Certificates: FLP 1004/2 Groups I II & III.

The photograph below shows a Controller type Flameproof Instrument, with a 4″ dial. Buxton Certificates: FLP 1053 Group I FLP 2690 Groups II & III.



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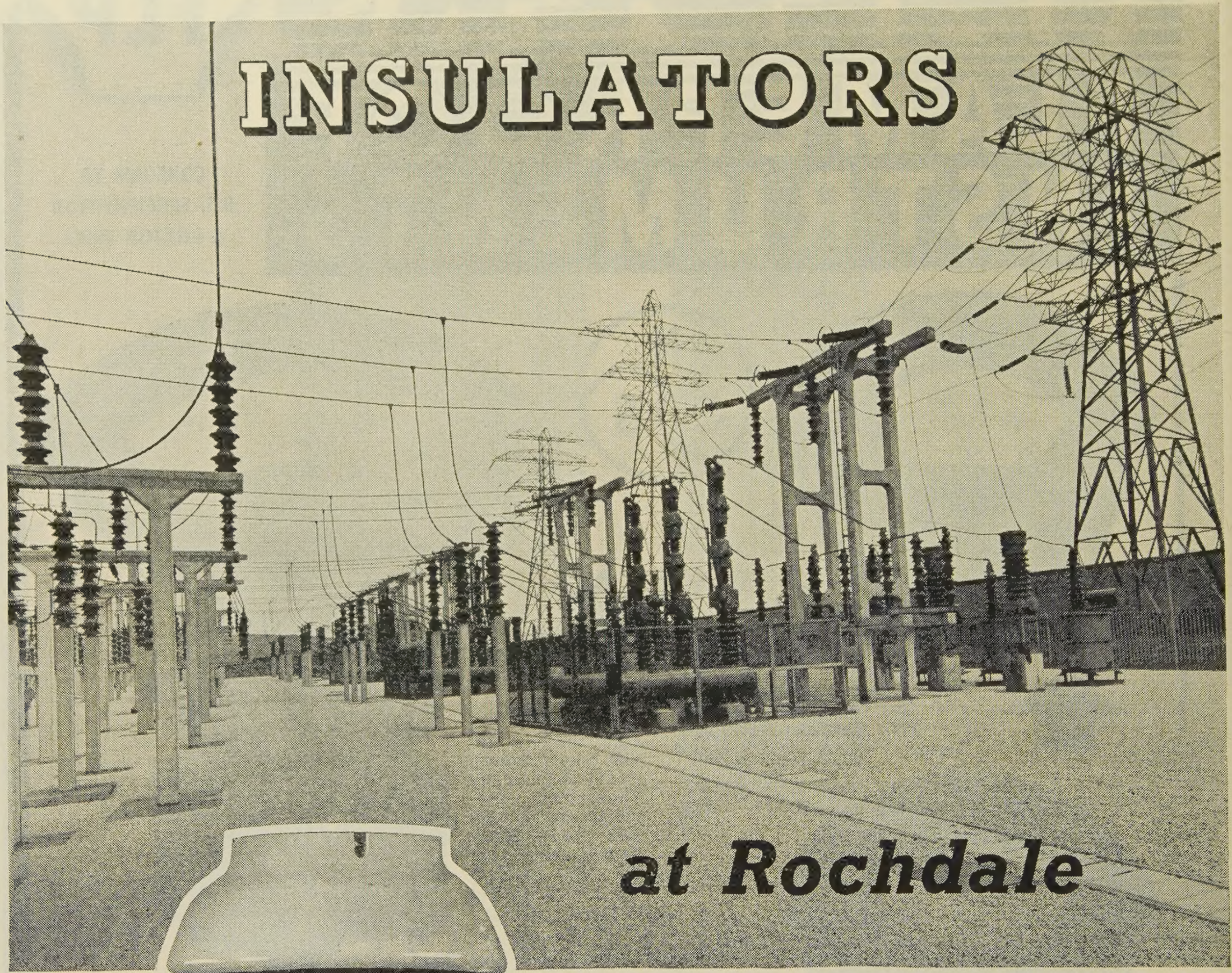
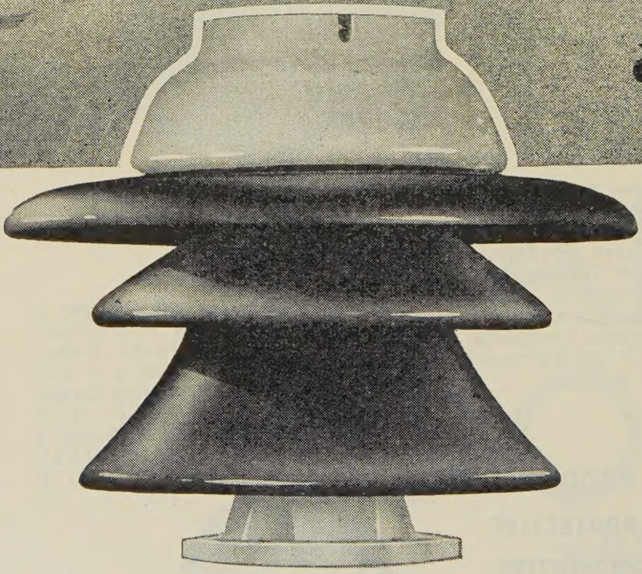


by courtesy of the English Electric Co. Ltd.



Doulton
Type BB9

Part of the sub-station of the Merseyside and North Wales Division of the C.E.G.B. at Rochdale, showing English Electric Air Blast circuit breakers and Doulton heavy duty 132 kV post insulators.

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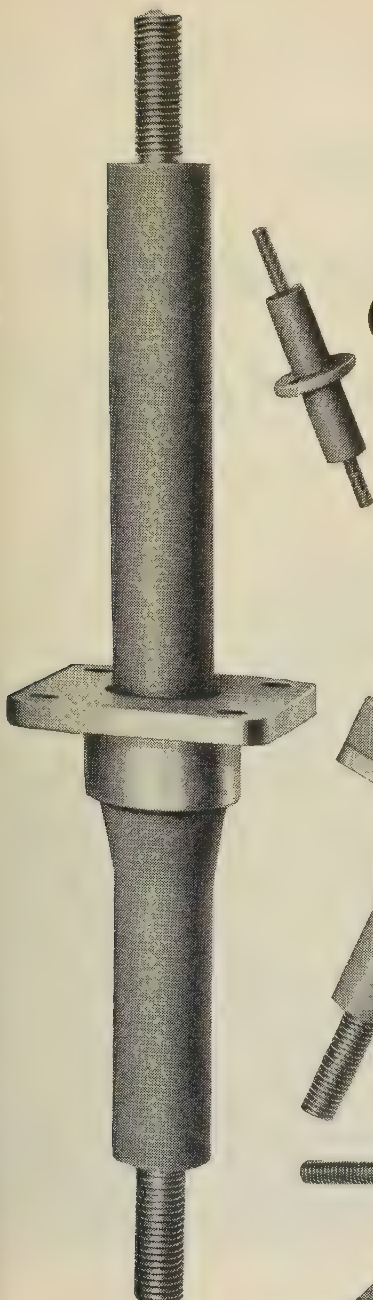

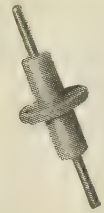

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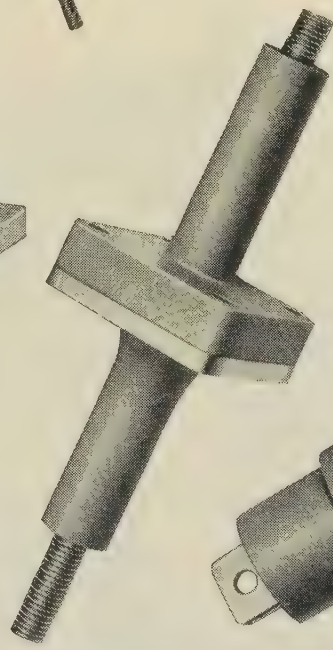

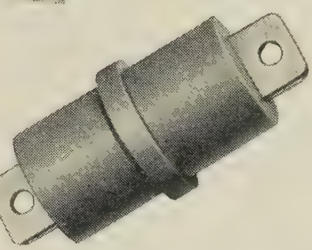

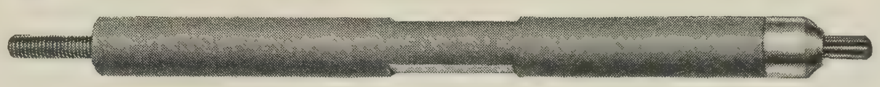

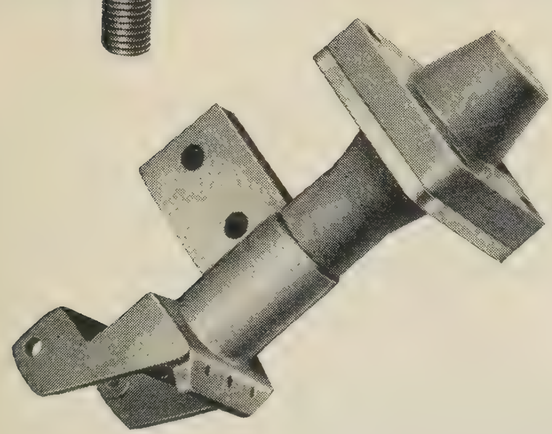

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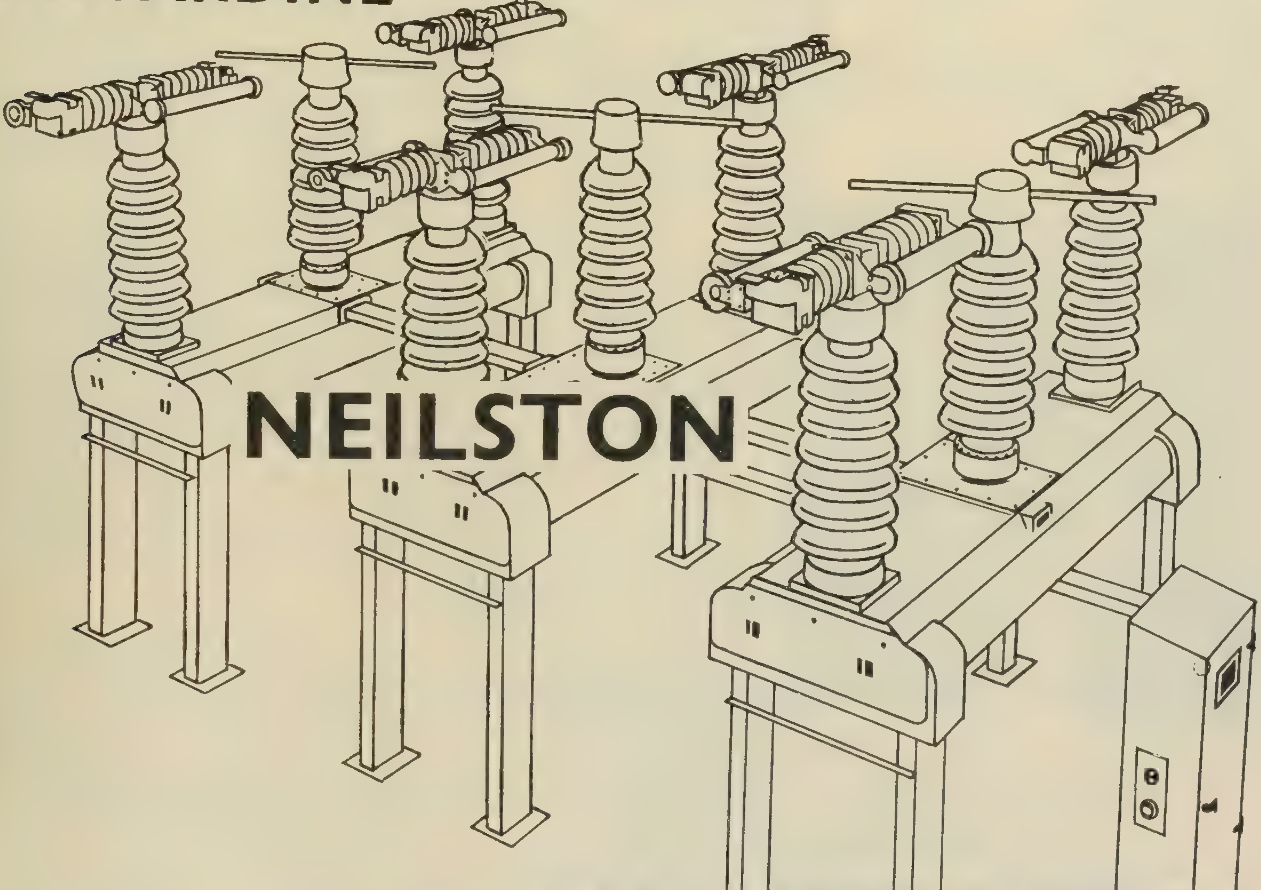

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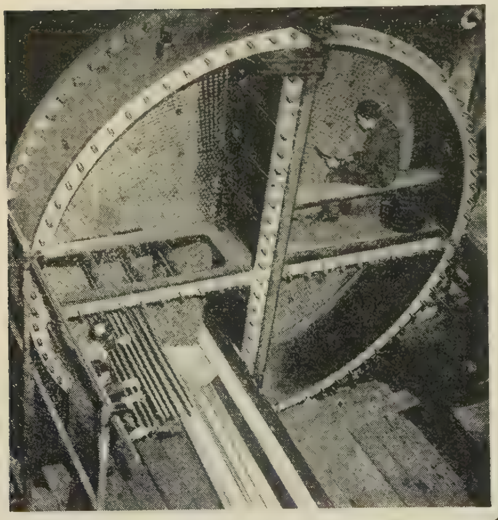
FOR THE ELECTRICAL INDUSTRY

Retubing a main condenser with thirty-five tons of SERCK Aluminium Brass condenser tubes at Deptford East Power Station.

Photo by courtesy of the Central Electricity Authority.



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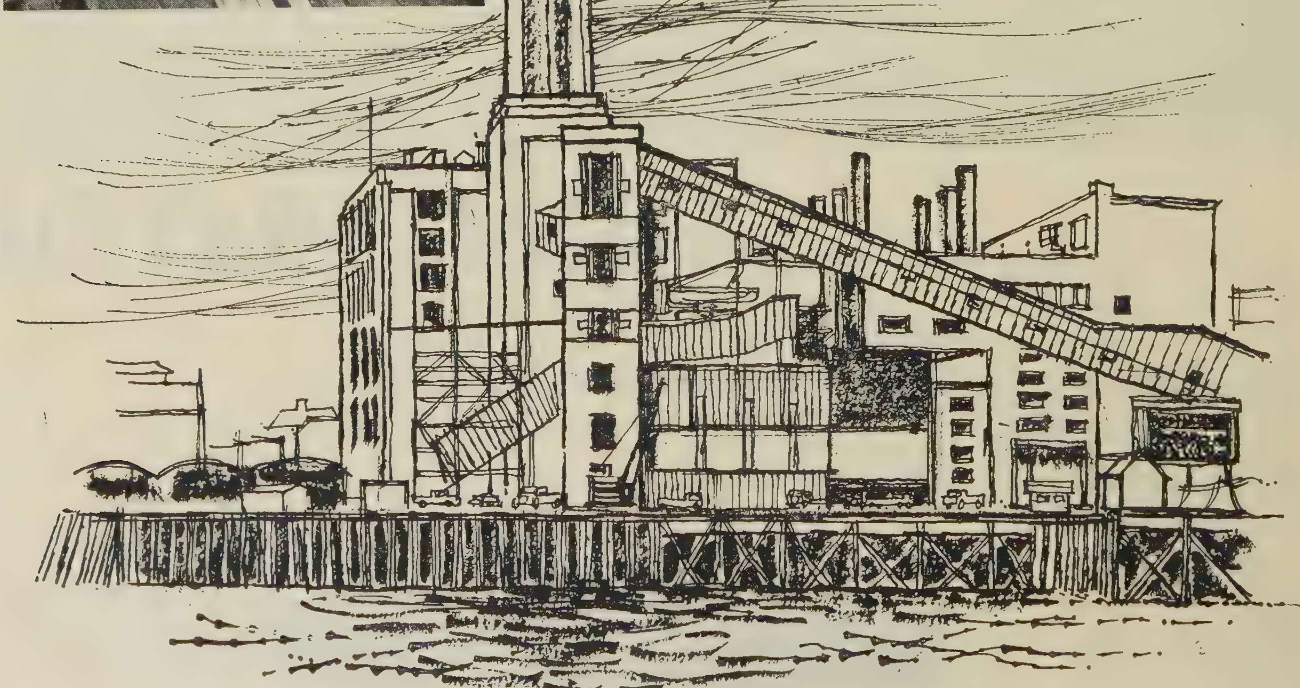
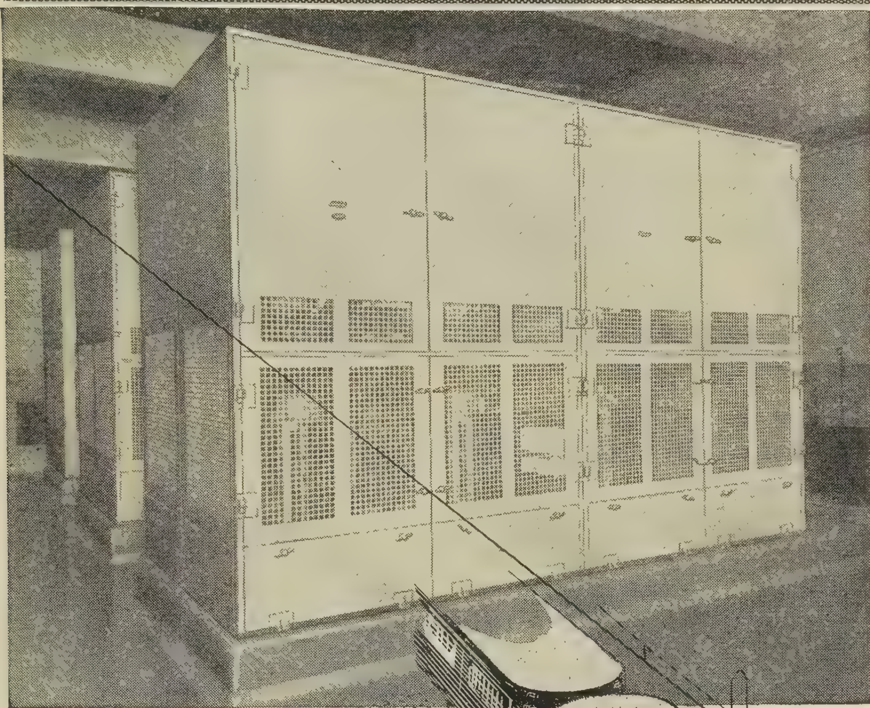
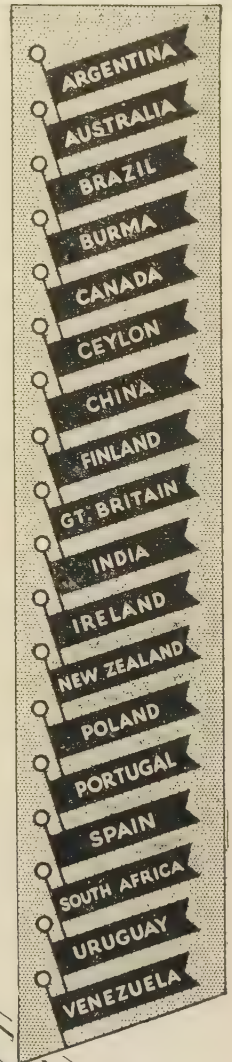


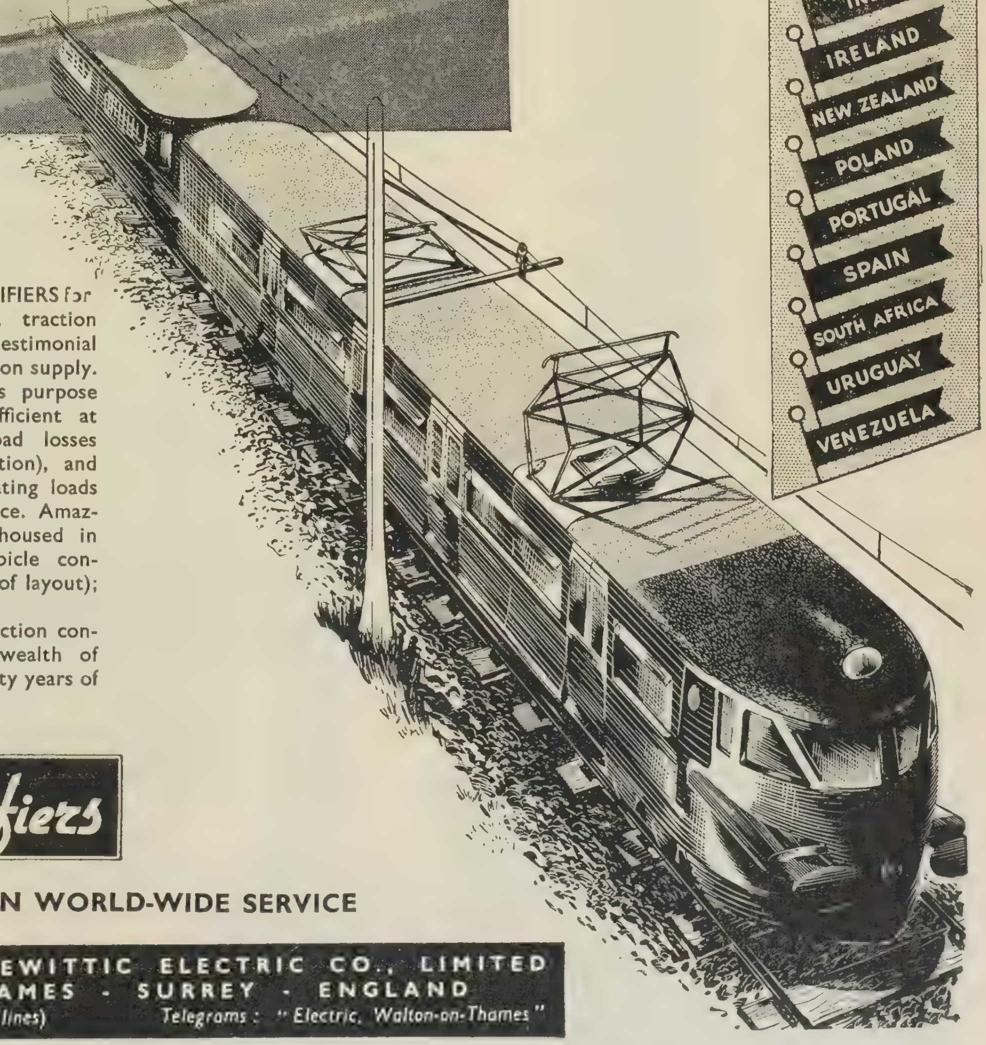
SERCK RADIATORS LTD · SERCK TUBES LTD | BIRMINGHAM 11

In Traction Service in 18 Countries



A typical Hewittic Rectifier substation (3,000 kW) for heavy traction supply.





THE selection of HEWITTIC RECTIFIERS for the operation of important D.C. traction systems in *18 countries* is in itself a testimonial to their eminent suitability for traction supply.

They have proved ideal for this purpose because they are exceptionally efficient at all loads, with very small no-load losses (assuring great economy in operation), and are unaffected by the heavy fluctuating loads and short circuits of traction service. Amazingly simple to install; normally housed in *unattended* substations; of unit-cubicle construction (affording great flexibility of layout); robust electrically and mechanically.

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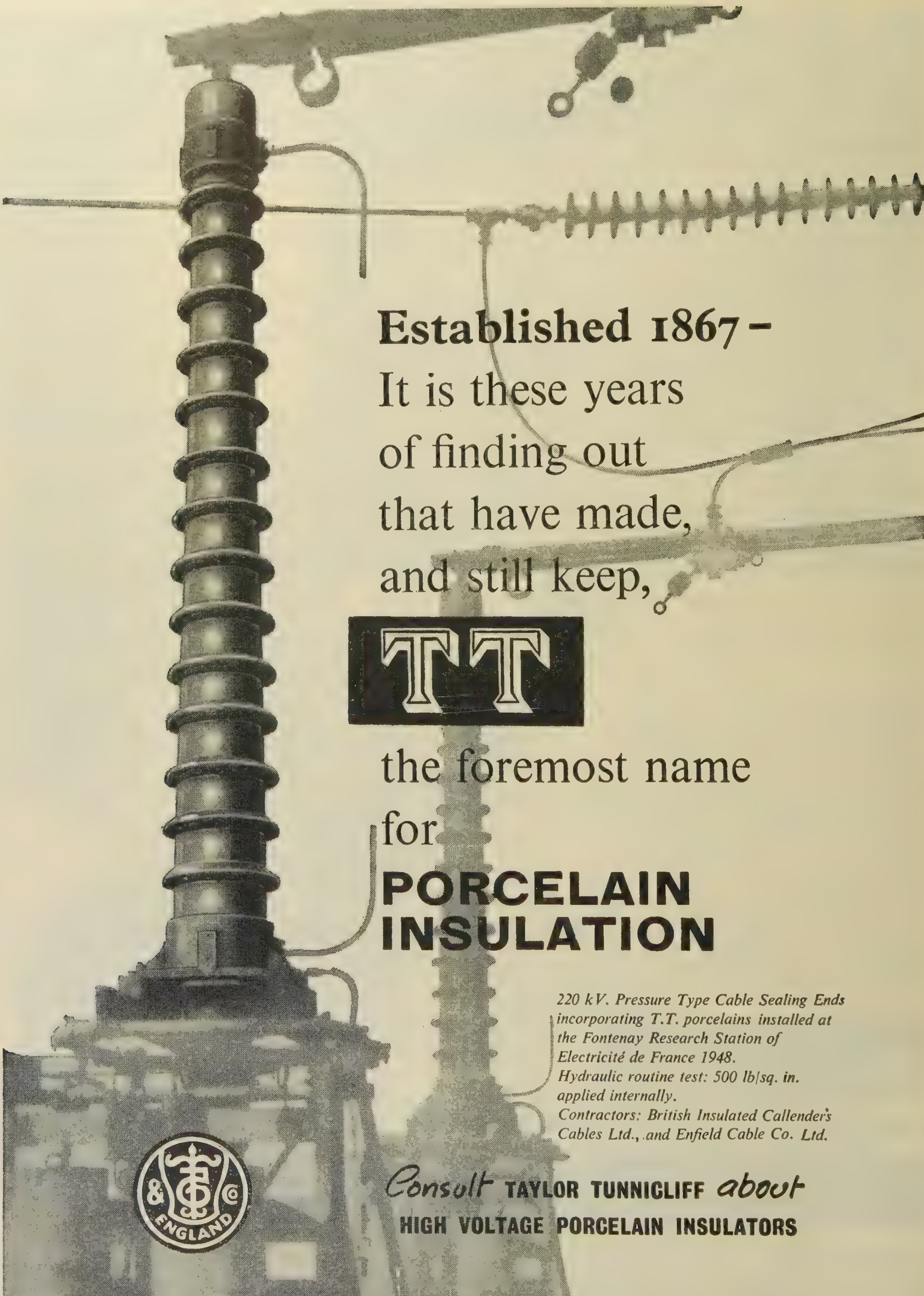

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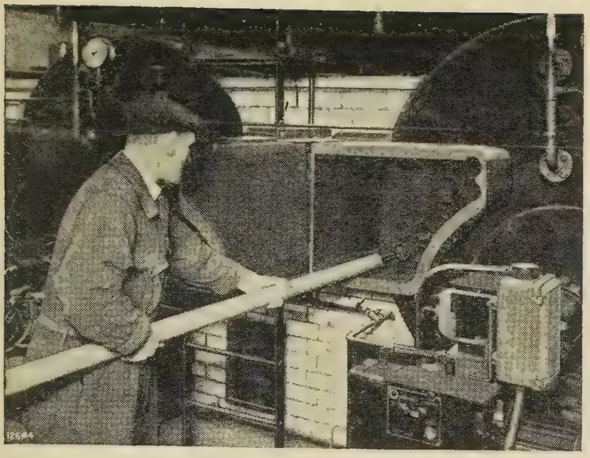
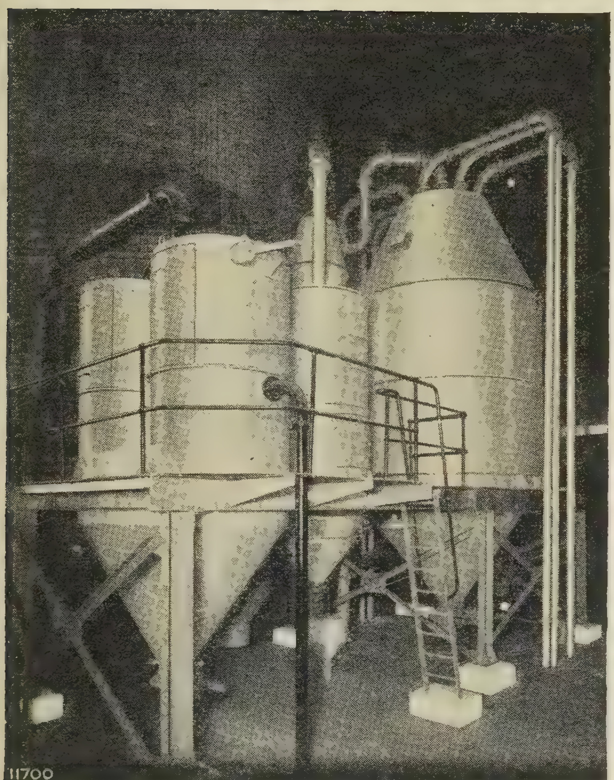


An oil-fired boiler being vacuum cleaned.

Boiler Flue Cleaning



Dust settling apparatus in a London Power Station with Sturtevant central vacuum cleaning and boiler flue cleaning plant.

Sturtevant Central Vacuum Cleaning Plant and Portable Machines are widely used for flue and boiler cleaning in stoker, P.F. and oil-fired boiler plants of all sizes.

INCREASED BOILER AVAILABILITY

Sturtevant central cleaning plants have a very high cleaning capacity and embody the most efficient dust settling apparatus. Not only is boiler availability increased, but large volumes of flue dust are simply and cleanly extracted, thus transforming an otherwise laborious and objectionable task into a clean operation.

For further particulars write to our reference N/101/26.

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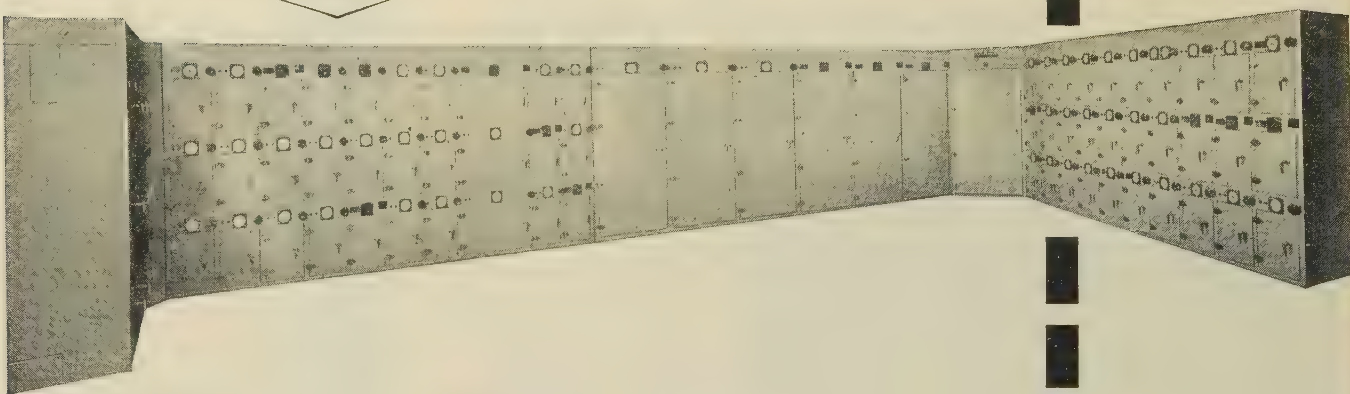
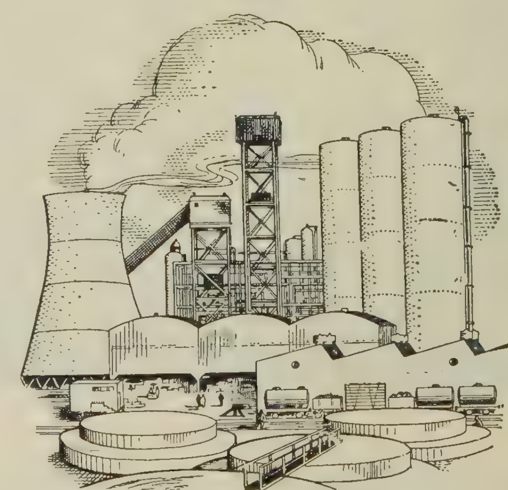
—all controlled from one central panel



This Dewhurst panel, recently installed in an extensive chemical plant situated in the Midlands, provides positive central control of 75 motors, ranging from ½ to 50 h.p. located in various processing departments of the plant.



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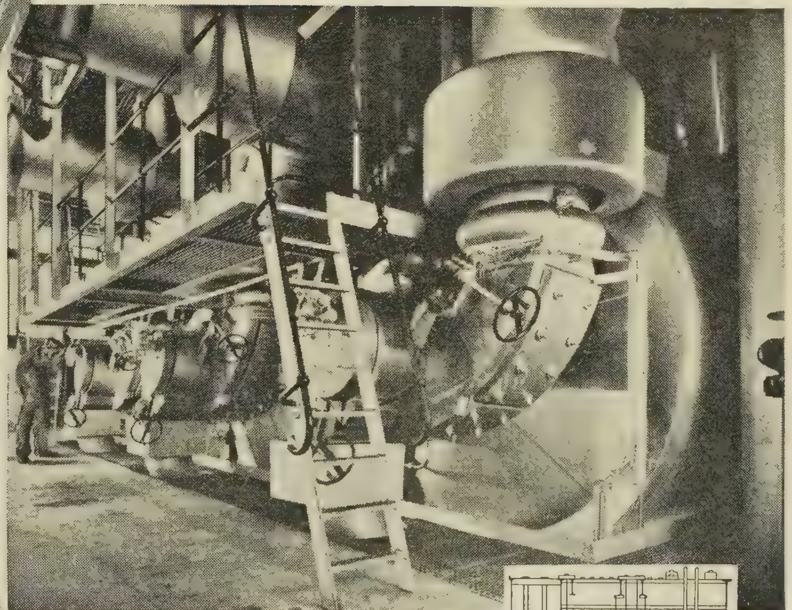
BOILER Nº 44

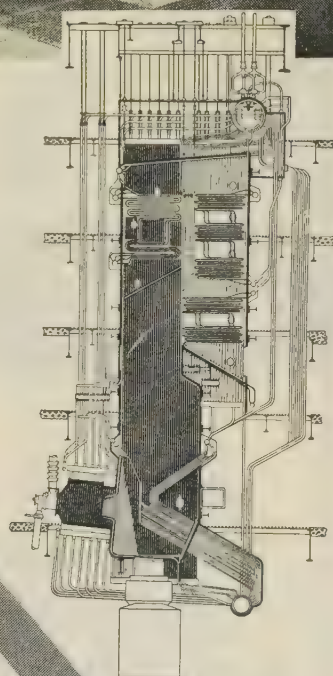


First Cyclone-fired central-station boiler in Britain . . .

Barking, with its A, B and C stations is one of the largest single concentrations of central-station generating plant in Europe, with a total installed capacity of $\frac{3}{4}$ million kilowatts. All the boilers at Barking are BABCOCK units and the latest, No. 44, is the first central-station unit operating in Great Britain with Cyclone-firing. This is a Radiant boiler with an output of 540,000 lb./hr. at 950 lb./sq. in. and 940°F, equipped with three 8-ft. diameter Cyclone Furnaces burning crushed coal, with axial entry.

Solves the ash-disposal problem

The Cyclone Furnace, by converting the ash to a granular slag that is easily handled and readily usable on civil works, provides an effective, economic solution of the ash-disposal problem. It burns a wide range of coals with high efficiency; improves boiler-availability; is safe, flexible in operation and well-suited to simple automatic combustion control.



Over 100 BABCOCK Cyclone-fired boilers

are ordered or in service in various countries. In Britain, units in service, under construction or on order, range from large industrial boilers of 200,000 lb./hr. up to central-station units of 860,000 lb./hr. to steam 120 MW generators.

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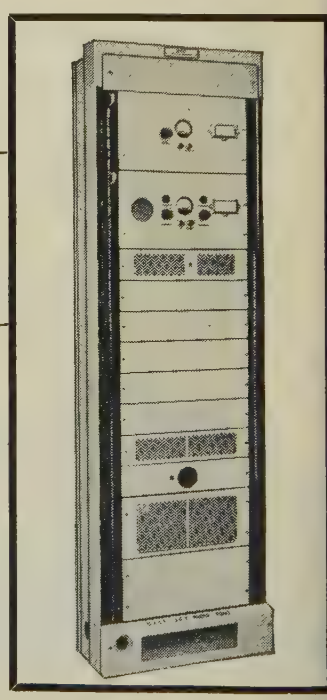

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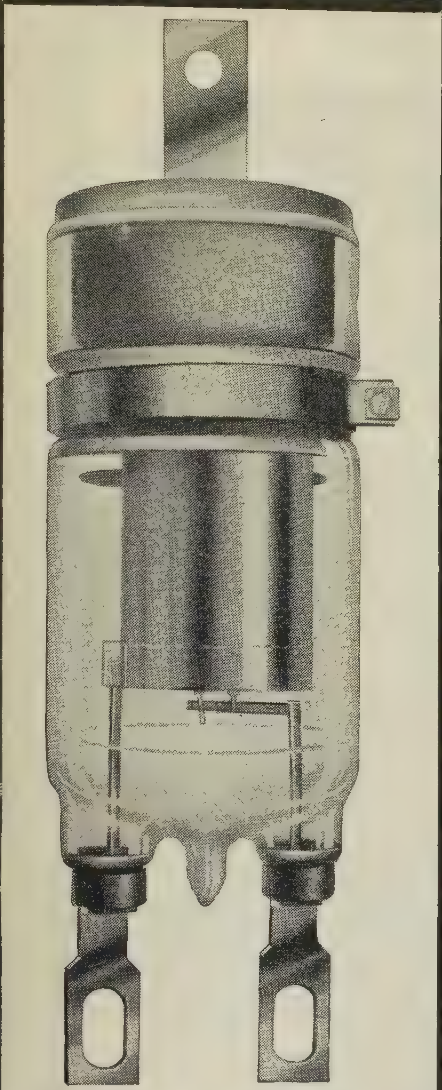

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6 and 12 amp Xenon Triode Thyratrons

New Disc-Seal construction gives these positive advantages

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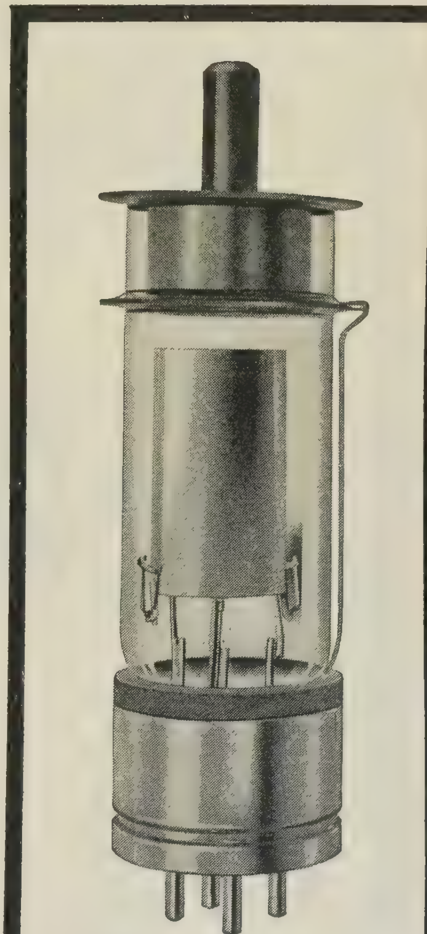

XR1-6400A

The basic advantages of Xenon thyratrons are enhanced in the Mullard XR1-6400A and XR1-12 by a new type of construction which provides improved electrical and mechanical performance.

The XR1-12 will continuously control currents of up to 12 amps, while the maximum capacity of the XR1-6400A is 6.4 amps. These two valves are rugged and their long life is backed by a year's guarantee.

Their heating-up time is only one minute and they will operate efficiently over a wide range of ambient temperatures.

Both types can be employed with confidence in a variety of power control applications including direct welding control, lighting control, motor control, electronic switches and regulated power supplies. Write to the address below for full data.

Type No.	American Type No.	Vf (V)	If (A)	va(pk) max. (kV)	P.I.V. max. (kV)	ik (pk) max. (A)	ik (av.) max. (A)	Heating-up time (secs)
XR1–6400A	6807	2.5	21	1.5	1.5	80	6.4	60
XR1–12	5855	2.5	34	1.5	1.5	150	12.5	60

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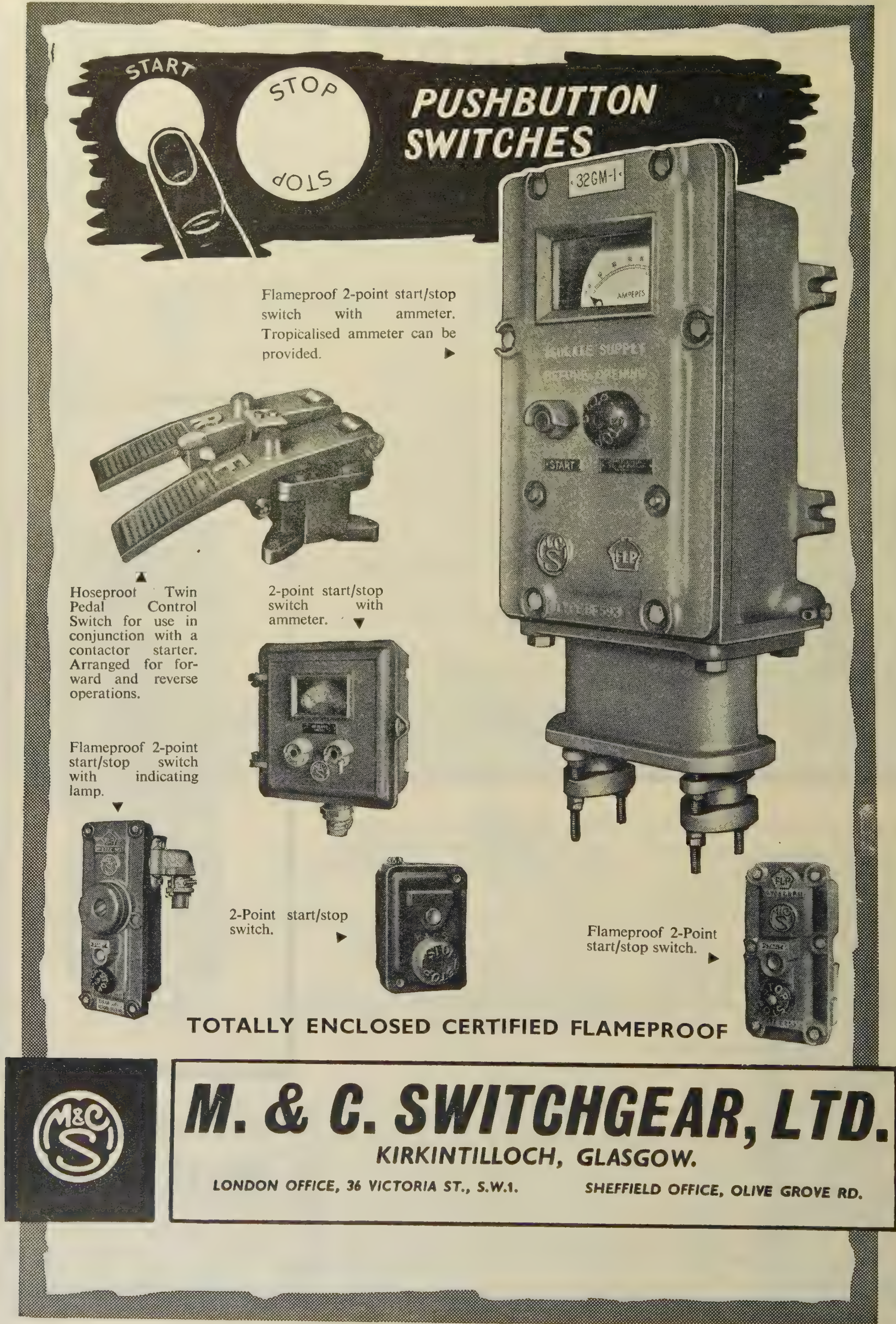

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Precipitator for world's largest boiler unit to be built by

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Removing 12 tons of dust an hour

At the new Thorpe Marsh power station, to be built for the Central Electricity Generating Board, Simon-Carves have been entrusted with the design and construction of the electro-precipitation plant which will have what is believed to be the heaviest duty yet proposed for precipitators on a single boiler unit anywhere in the world.
The unit will have an output of 550 MW and the boiler is expected to produce more than one and a half million cubic feet of flue gas per minute. The plant will remove fine dust, which would otherwise pollute the atmosphere, at a rate of about 12 tons an hour.

PREVENTING AIR POLLUTION

MW per unit	STATION	Gas volume per boiler (cubic feet per minute)
550	THORPE MARSH	over 1,500,000
120 120 100	PADIHAM 'B' DRAKELOW 'B' WILLINGTON 'A'	over 300,000
60 60 60	HAMS HALL 'C' BOLD 'B' ROGERSTONE	over 200,000
60 30 30 30	CHADDERTON 'B' FLEETWOOD HACKNEY 'B' HUNCOAT	over 100,000

This table of Simon-Carves precipitators for power stations of the Central Electricity Generating Board emphasises the magnitude of the Thorpe Marsh project. High average efficiency on plants already tested enables Simon-Carves to accept this great responsibility with complete confidence.

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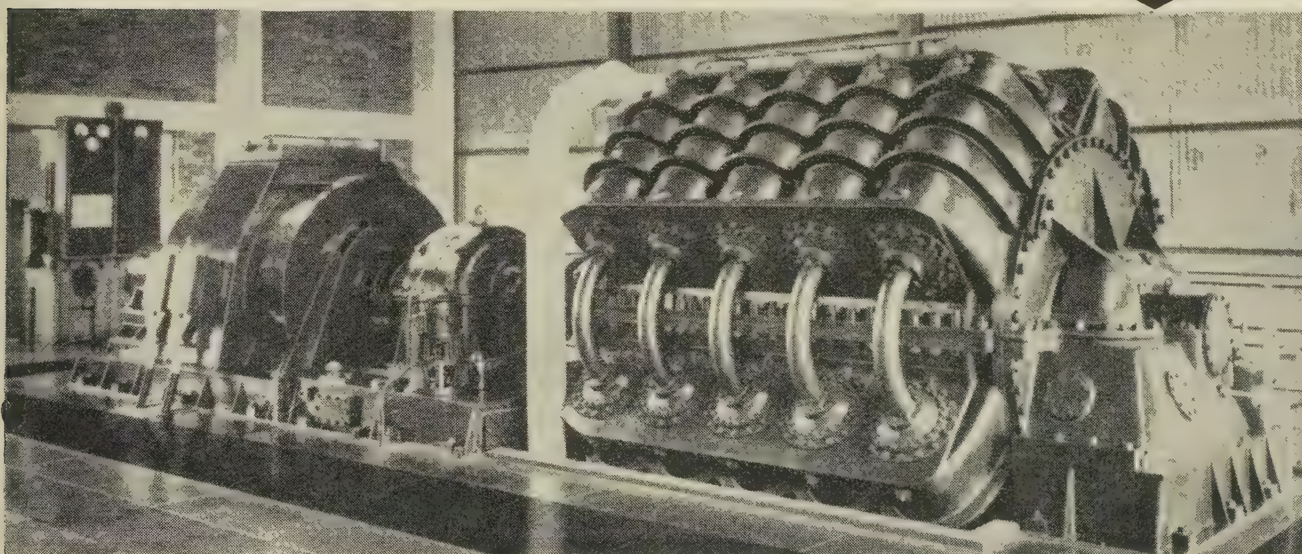
**The design of the machine embodies Oerlikon patents Nos. 54268 and 558910.*



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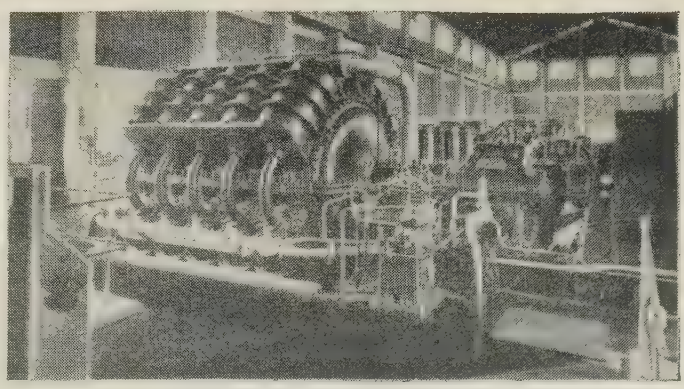

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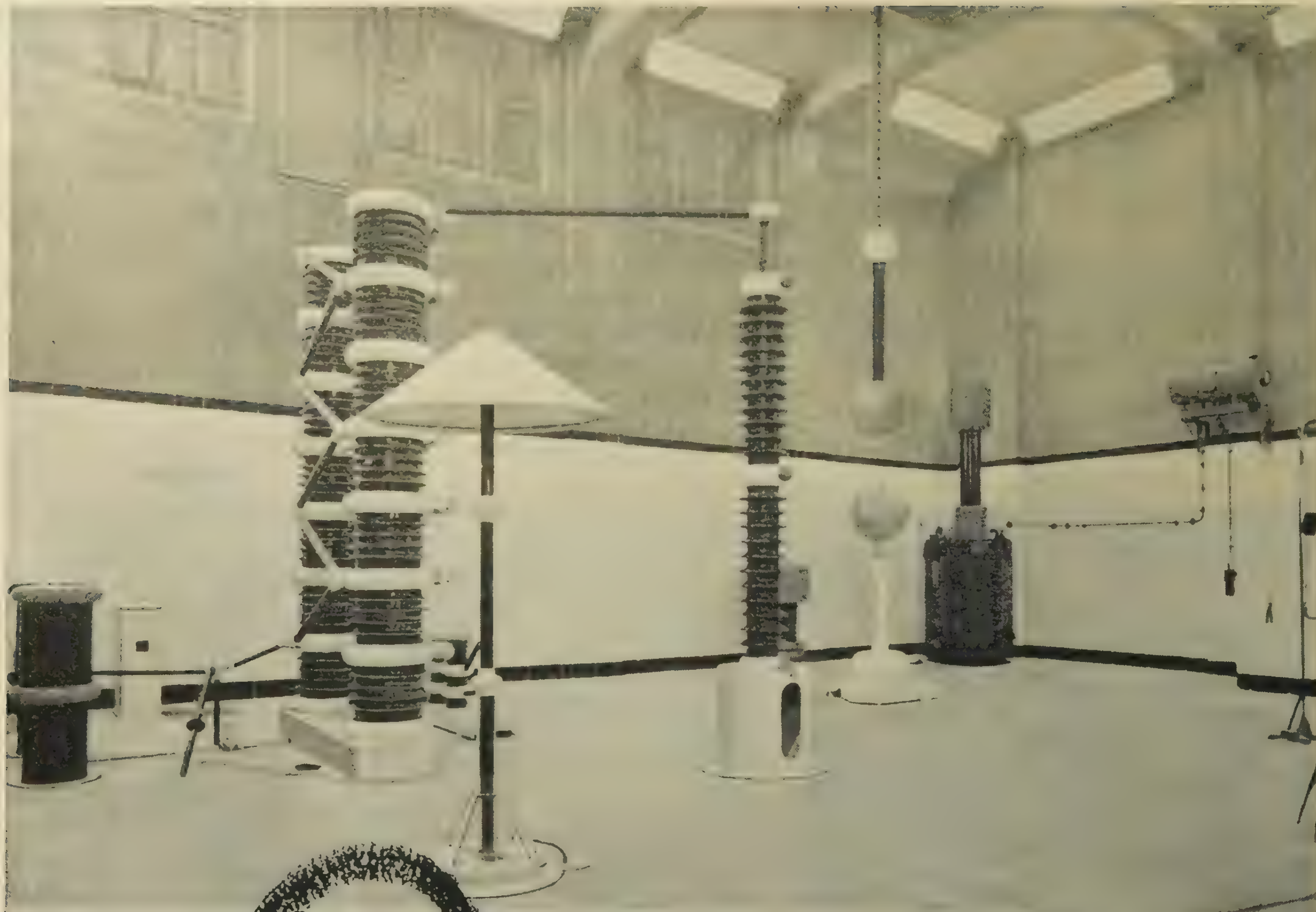

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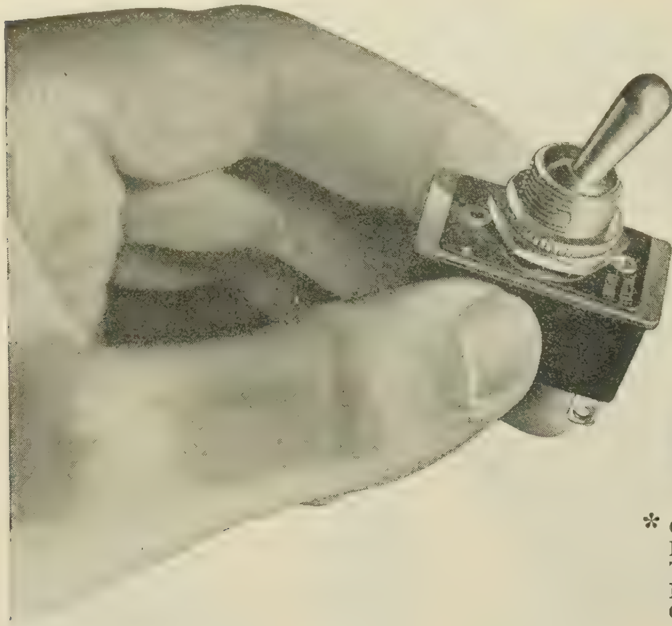

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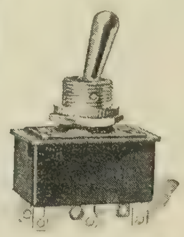

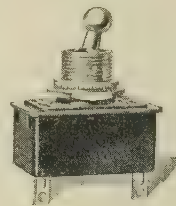

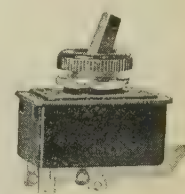

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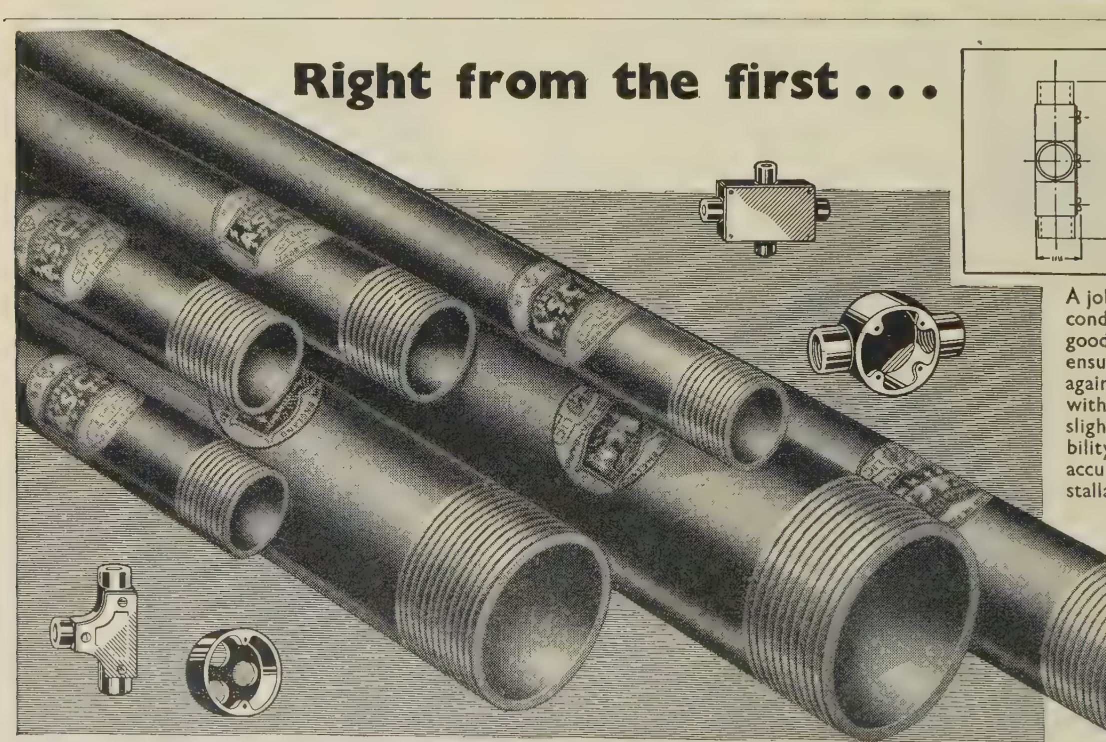

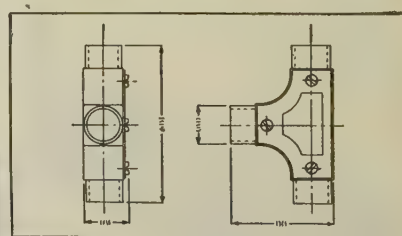

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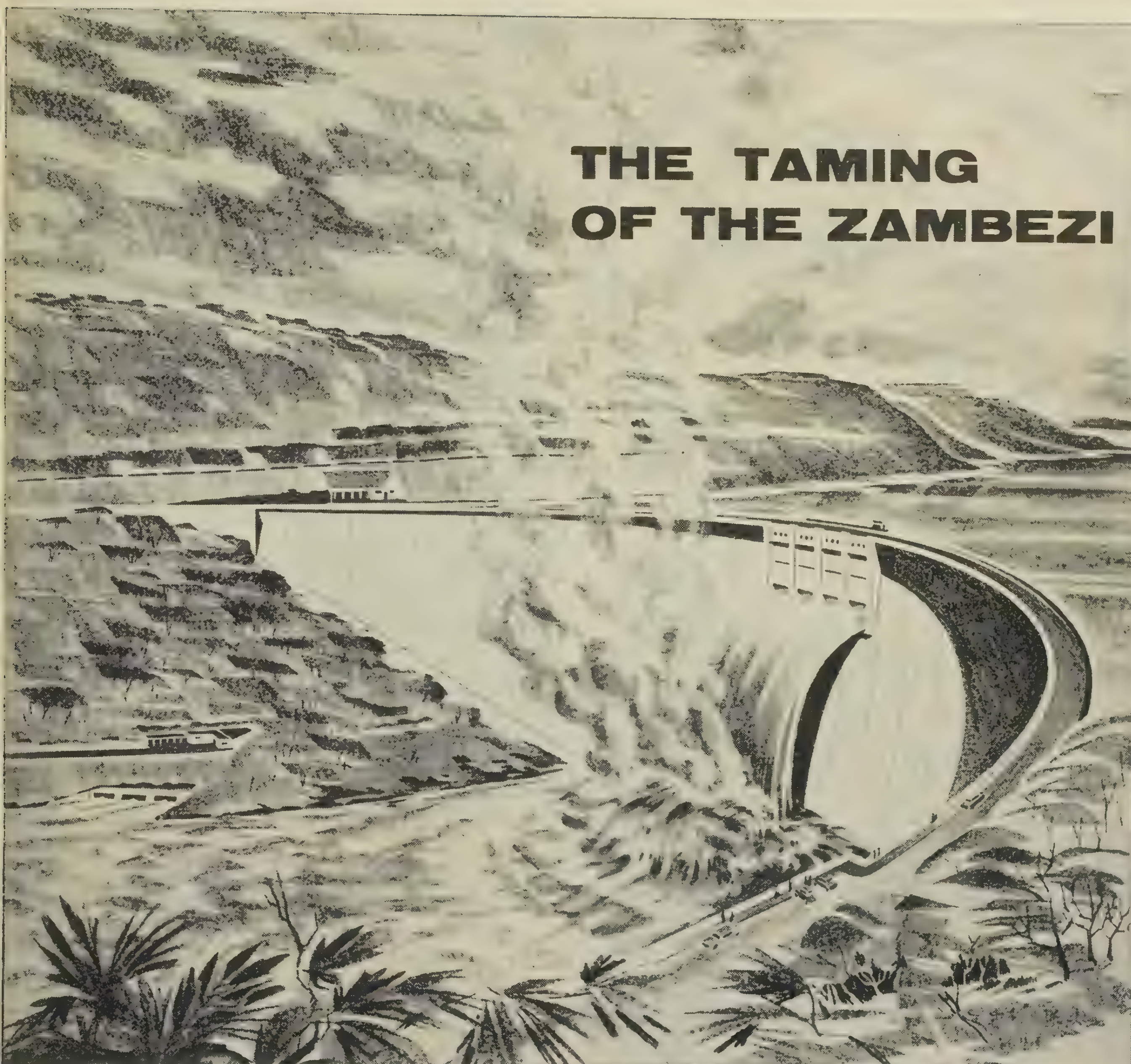

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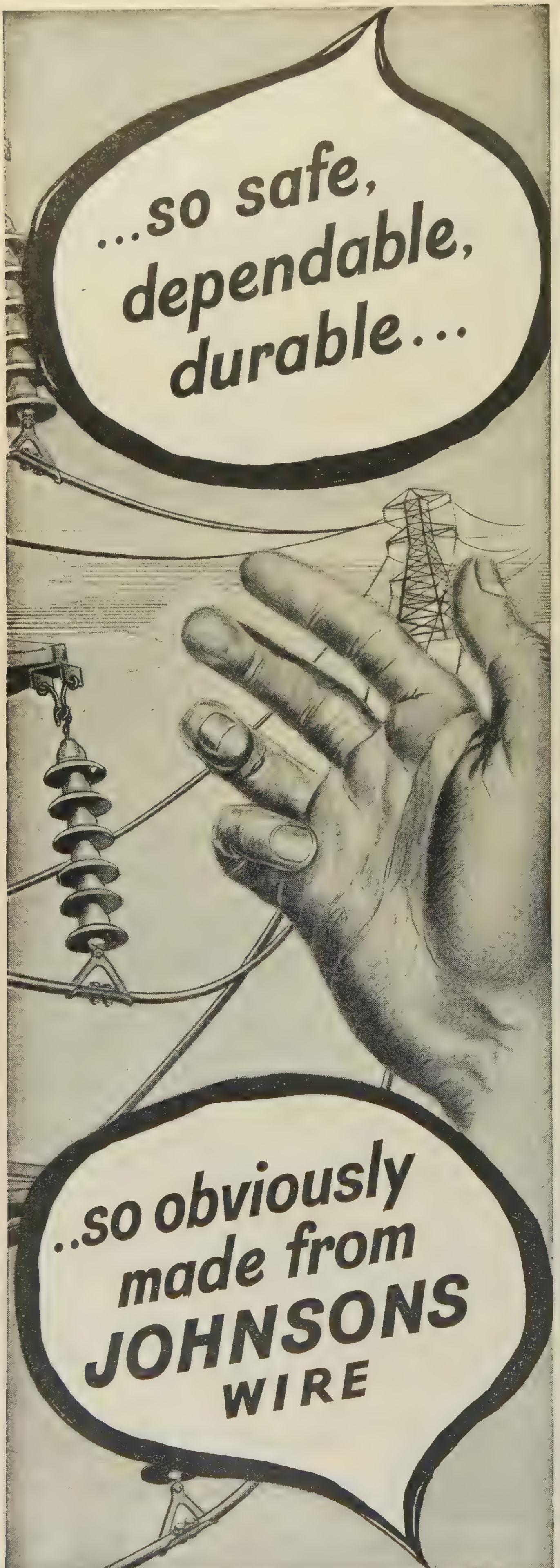

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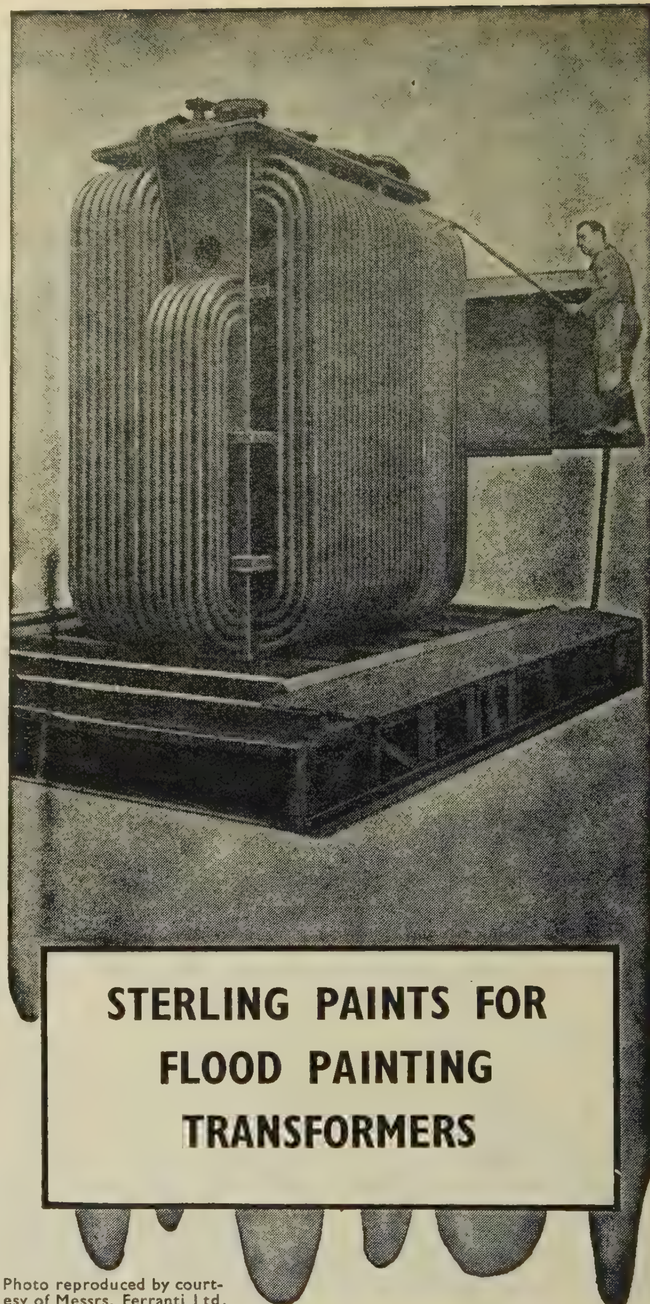

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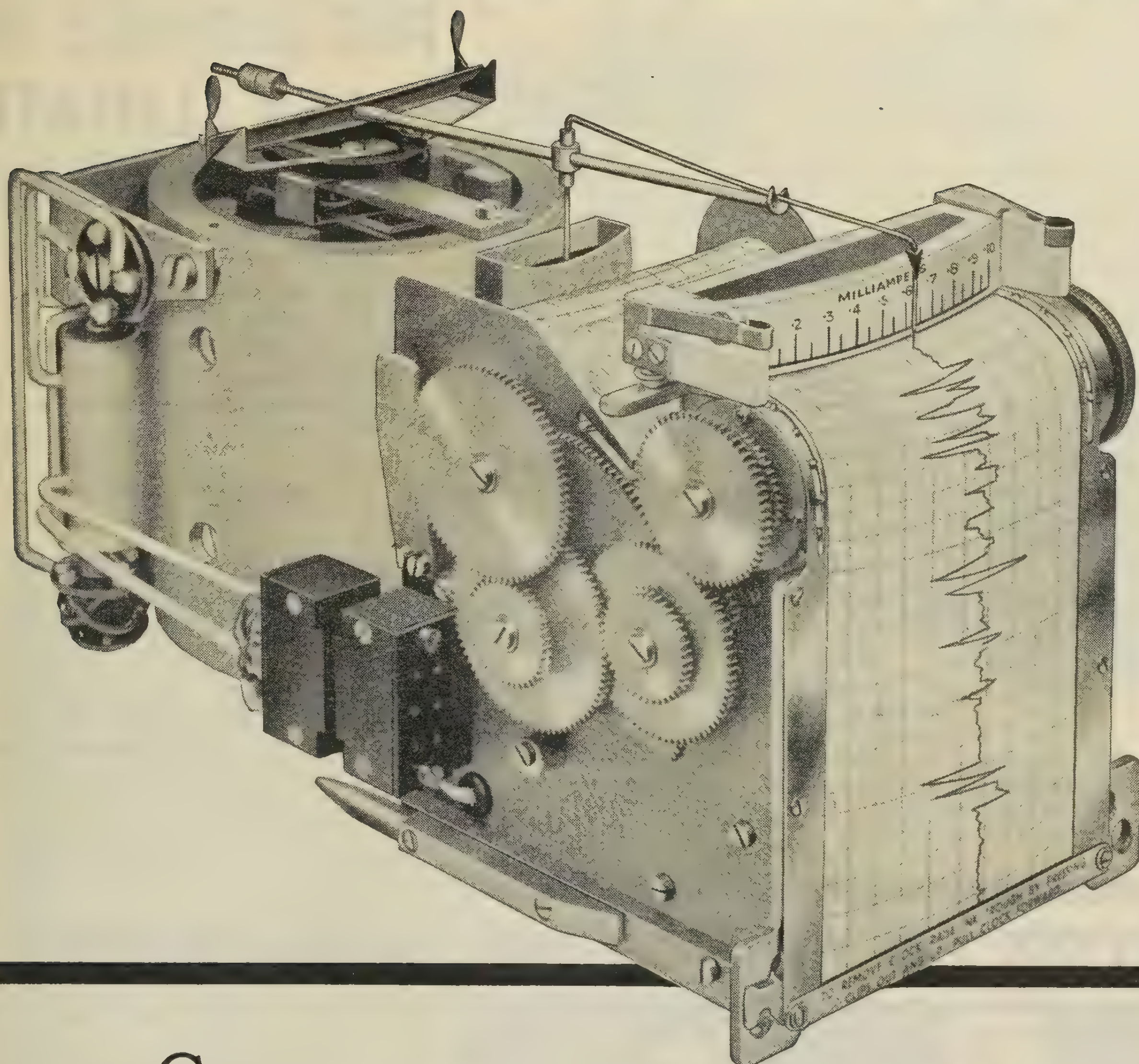

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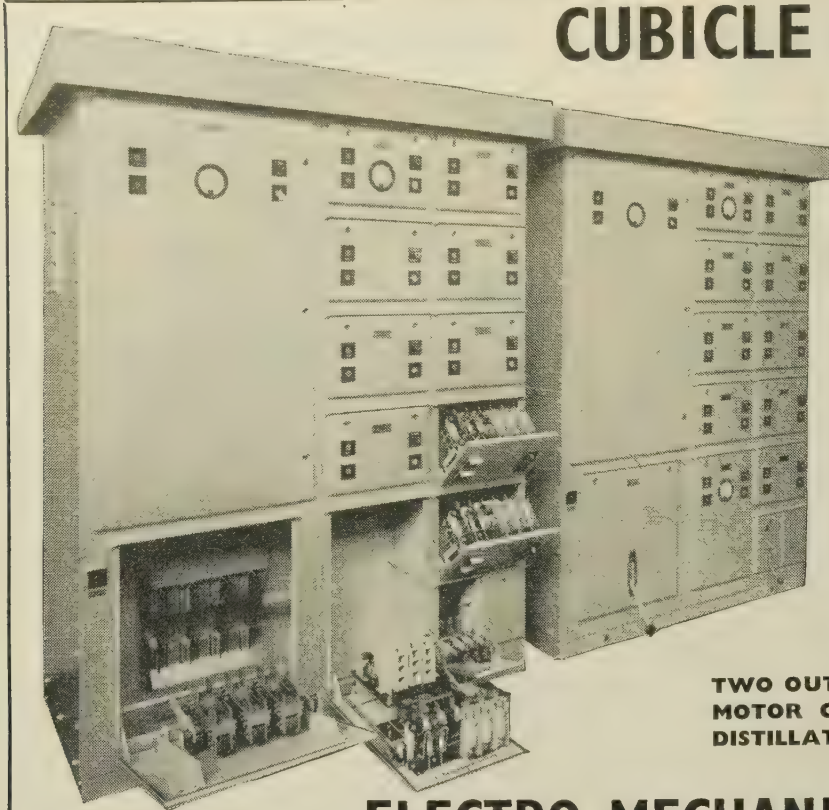

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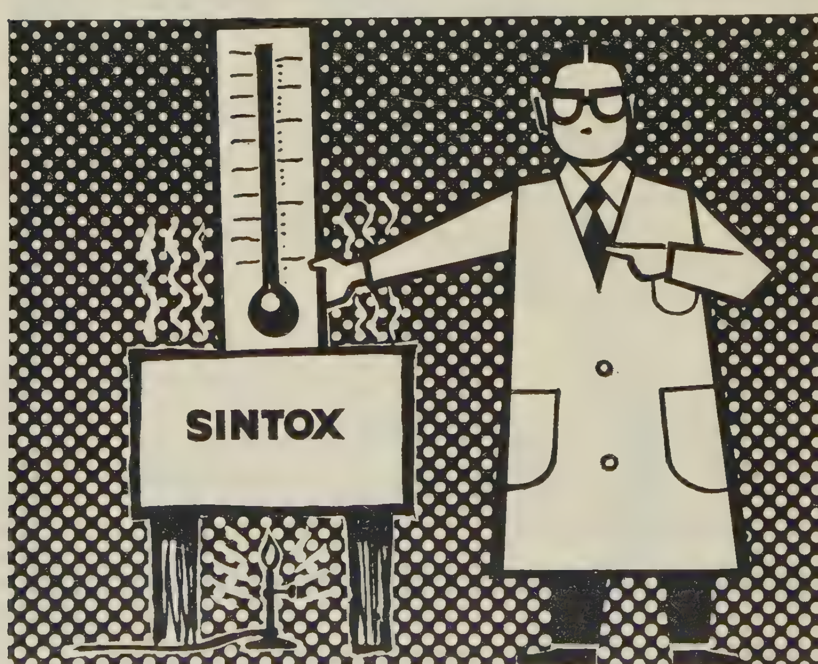

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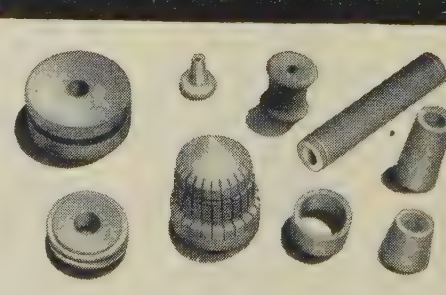

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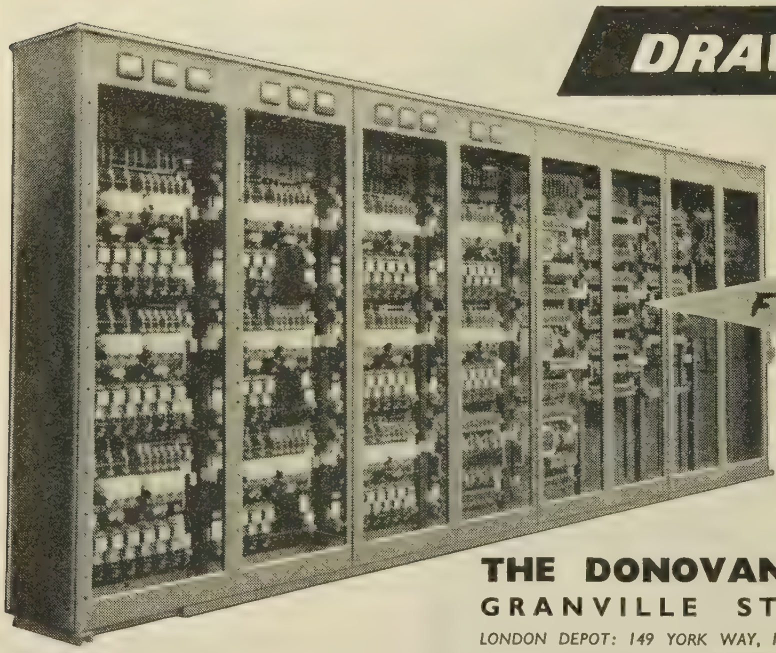

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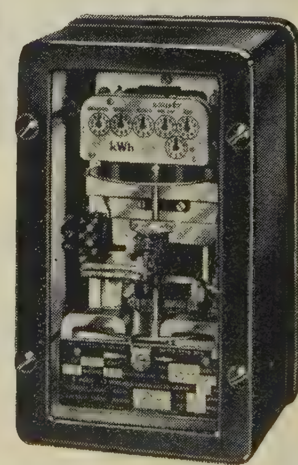

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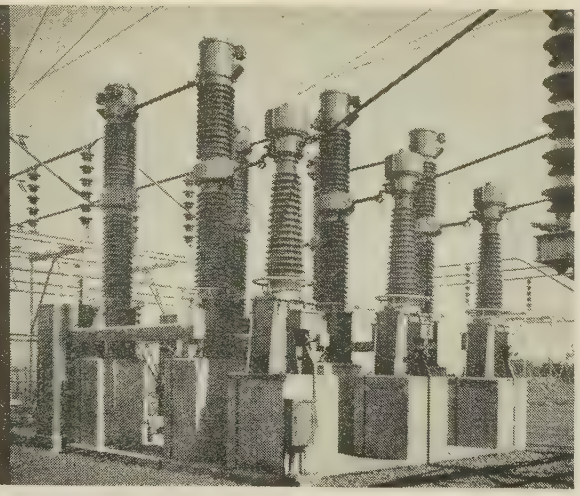

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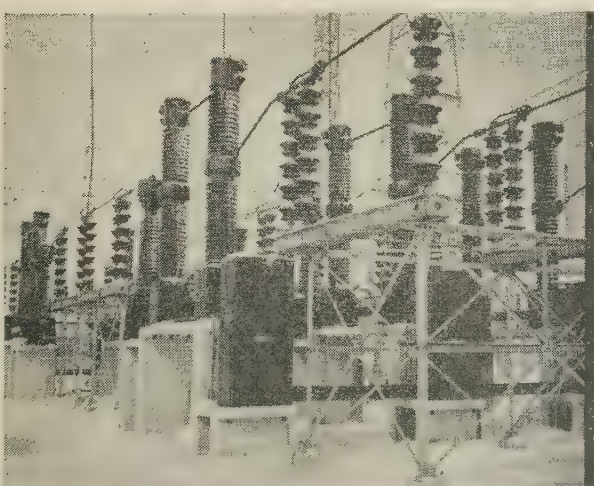

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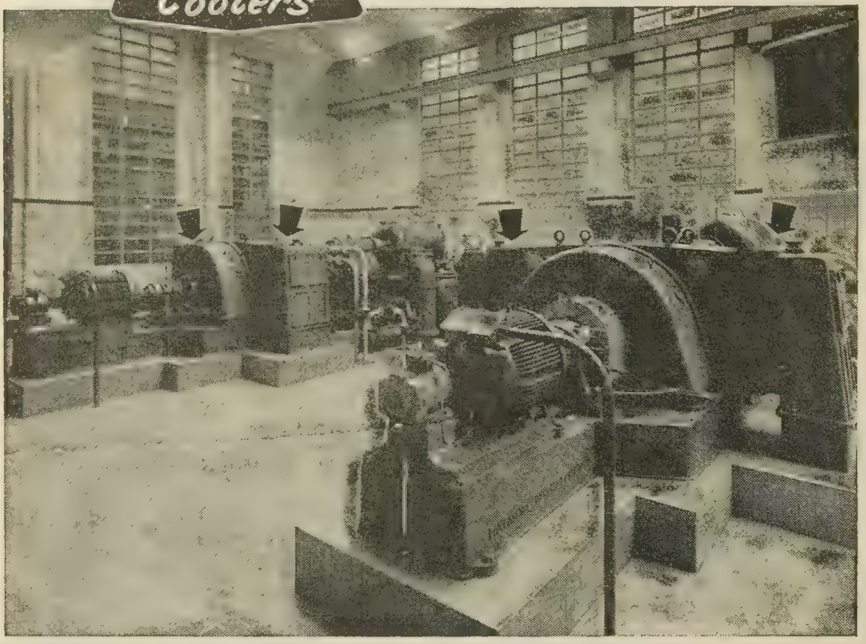

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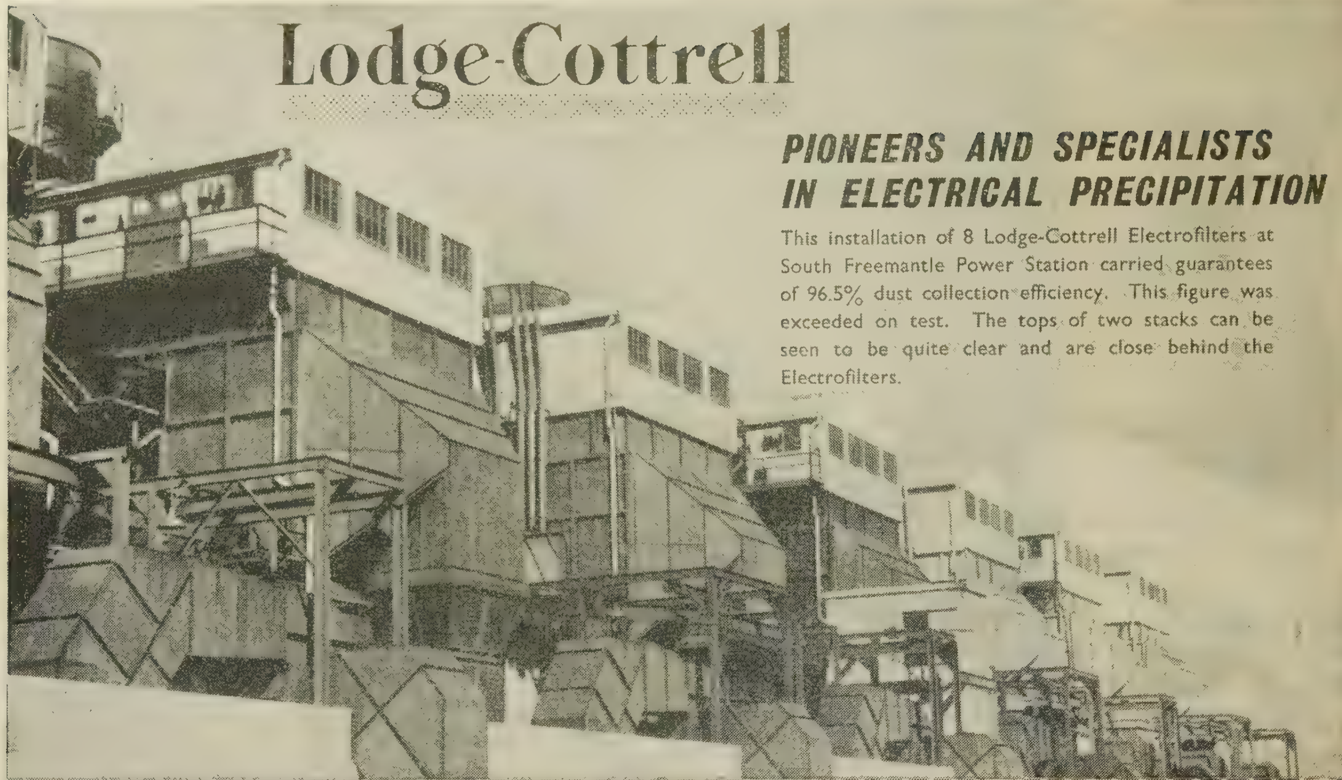

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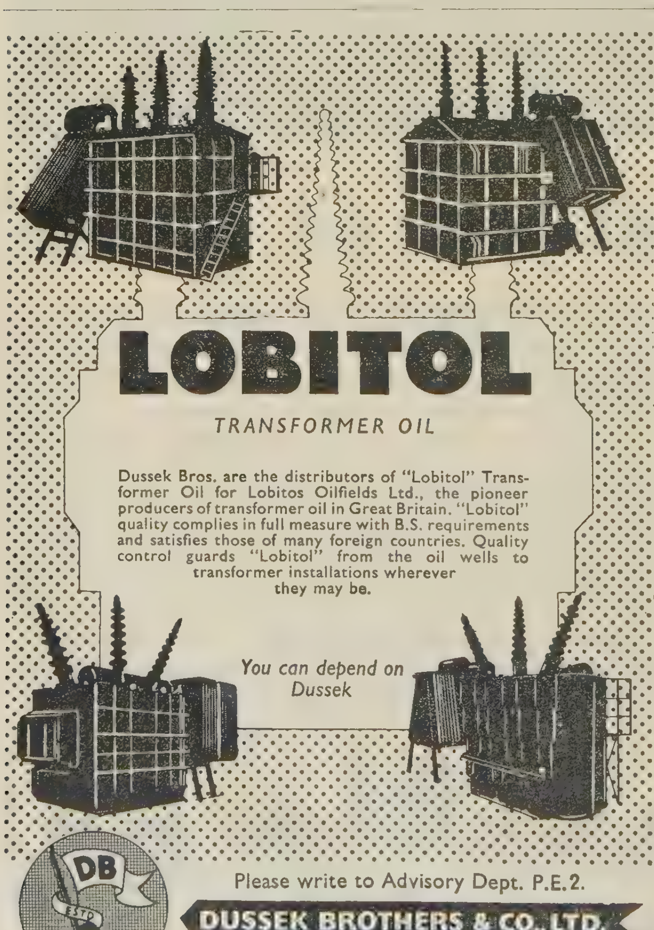

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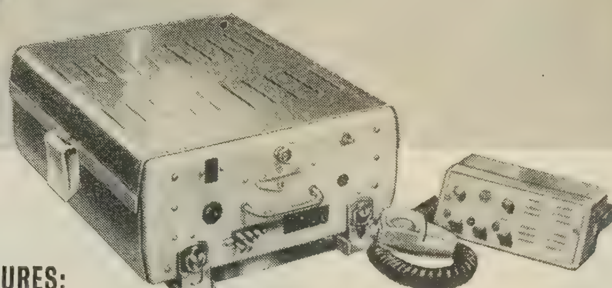

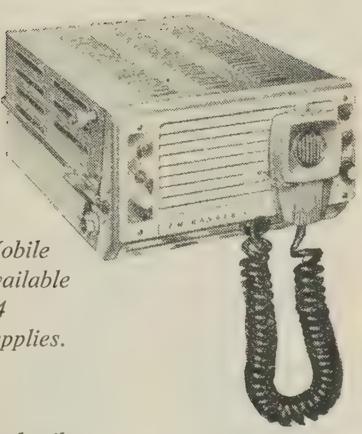

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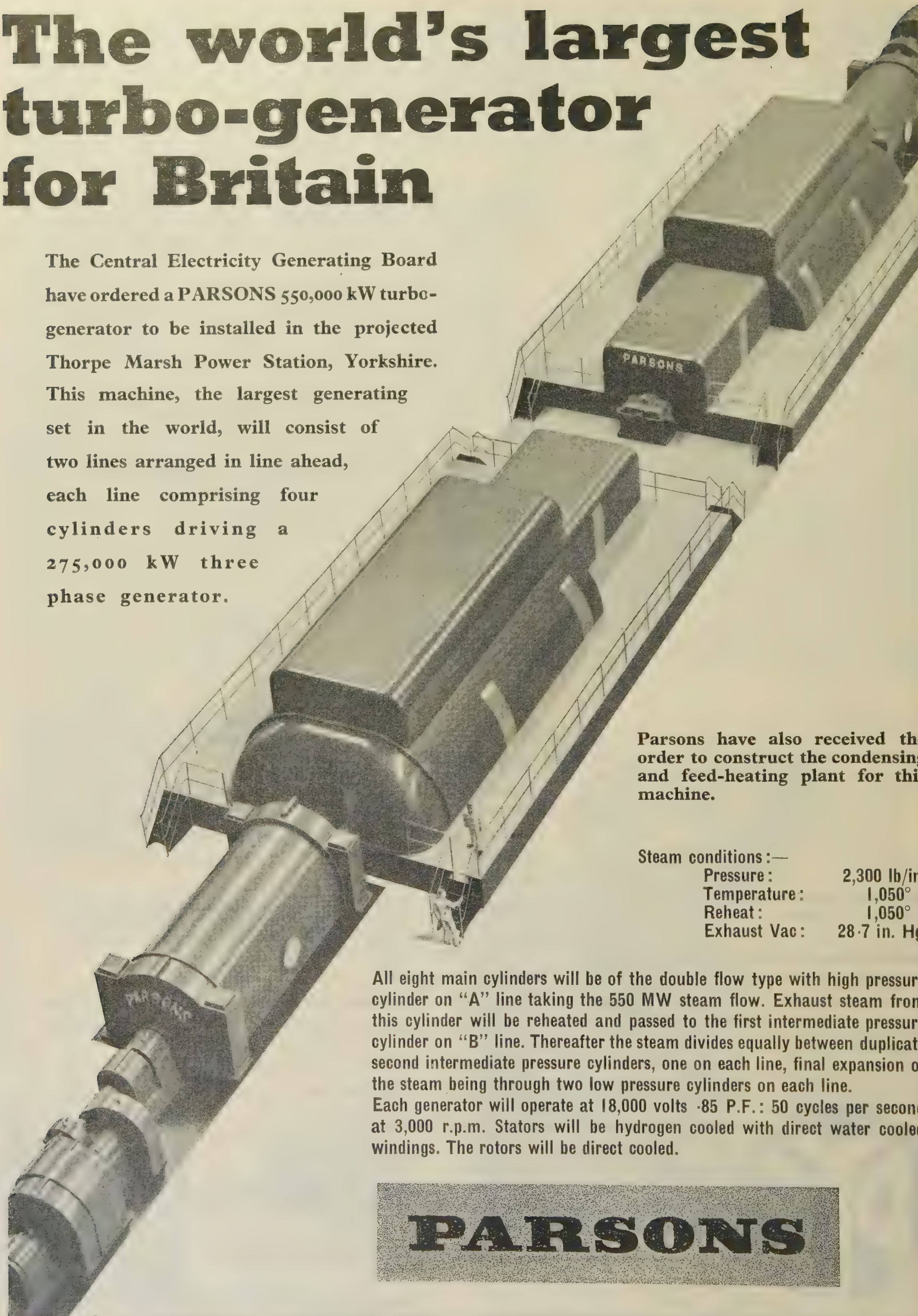

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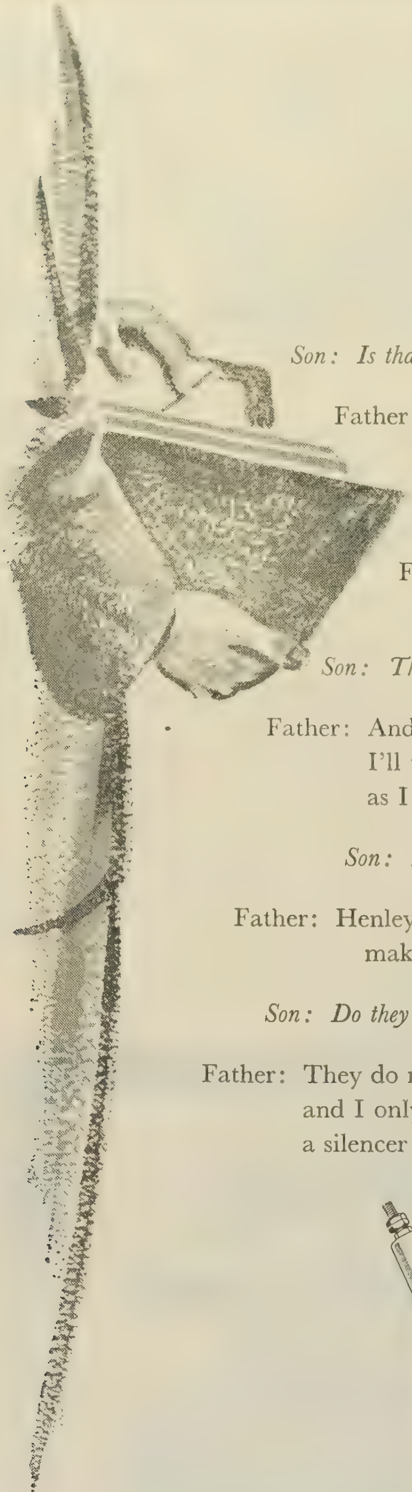

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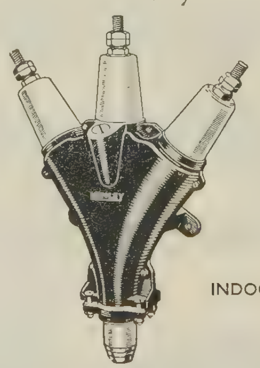
Father: Henley's are a large firm — they make these things.

Son: Do they make Roman Candles, too?

Father: They do not. They make electrical things, and I only wish their range included a silencer for small boys!



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Nov. 1957
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THE DEVELOPMENT OF VARIABLE-SPEED HIGH-POWER DRIVES FOR LARGE WIND TUNNELS

By P. McKEARNEY, O.B.E., A.M.I.Mech.E., Member, L. S. DRAKE, A.M.I.Mech.E., Associate Member, and E. G. MALLALIEU, B.Sc., A.M.I.Mech.E., Associate Member.

(The paper was first received 12*th December*, 1956, *and in revised form* 17*th July*, 1957. *It was published in November*, 1957, *and was read before the* NORTH-WESTERN CENTRE 7*th January*, THE INSTITUTION 9*th January*, *the* RUGBY SUB-CENTRE 11*th February*, *the* WESTERN CENTRE 10*th March*, *and the* NORTH STAFFORDSHIRE SUB-CENTRE 5*th May*, 1958.)

SUMMARY

The paper outlines the power requirements of large wind tunnels, with particular reference to the compressor driving plant for high-speed and multi-purpose tunnels. After briefly reviewing available driving methods, with examples of typical installations on existing tunnels, the needs of a particular research establishment are described. There two or more large wind tunnels are being built, each requiring high powers at variable speed and using synchronous motors for the main driving power.

To meet these requirements, the case for a system consisting of a fixed-frequency supply from the Grid and a variable-frequency supply generated on site is discussed.

(1) INTRODUCTION

The rapid development in aircraft performance brought about by the last war showed clearly that the existing research facilities for investigating fundamental problems in aerodynamics and for model testing would have to be greatly extended. As the speed of aircraft approached the speed of sound there was a pressing need to study the problems of transonic and supersonic flight, but the supersonic wind tunnels in this country were few in number and small in size. The need was for larger supersonic tunnels in which Reynolds numbers of at least the same order of magnitude as those obtaining in actual flight could be reached and in which comparatively large-scale models of complete aircraft could be tested.

The aircraft industry itself, after the war, laid down a number of new tunnels of various sizes, and the Government planned to extend its own testing facilities by building a number of larger tunnels covering both low and high speeds at a new Government research establishment. Individual wind-tunnel drives of the order of 50 000 h.p. and upwards were required, and the total installed motor horse-power was expected to exceed 200 000 h.p. It was therefore necessary to examine the power requirements of the different types of wind tunnels to be built and to decide how they could best be met.

Messrs. McKearney, Drake and Mallalieu are at the Ministry of Works.

(2) POWER REQUIREMENTS

Ideally the conditions under which a model is tested in a wind tunnel should be dynamically similar to the free-flight conditions of the body of which the model is a scale copy. In practice, some factor, be it the power required to operate the tunnel, the cost of running the tunnel, the cost of building the tunnel or the strength of the model or its 'sting', makes it impossible to achieve complete dynamic similarity. Under these conditions it is usual to satisfy the most important of the similarity conditions and to tolerate discrepancies in others.

In low-speed tunnels, the compressibility of the air is unimportant and conditions can be arranged so that departures from dynamic similarity between the model in the tunnel and the full-scale body in free flight can be tolerated. In high-speed tunnels, however, the compressibility of the air has to be taken into account, and both the free-flight Mach number and the free-flight Reynolds number should be achieved in the tunnel. Although it is comparatively easy to obtain the free-flight Mach numbers, invariably the free-flight Reynolds numbers are not achieved, and it is the need to approach these more closely that has led to the development of high-power drives for high-speed wind tunnels.[1]

Wind tunnels can be divided into five types:

(*a*) Low-speed tunnels, in which the free-stream Mach numbers are less than 0·3.
(*b*) High subsonic-speed tunnels, in which the free-stream Mach numbers are less than unity but greater than 0·3.
(*c*) Supersonic tunnels, in which the free-stream Mach numbers are greater than unity.
(*d*) Transonic tunnels, in which free-stream Mach numbers greater and less than unity are both present.
(*e*) Multi-purpose tunnels, which combine two or more of the above types in one tunnel.

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(2.1) Low-Speed Tunnels

In low-speed tunnels the speed of the undisturbed air flow in the working section is less than $M = 0 \cdot 3$, i.e. about 250 m.p.h. at sea level, where M is the Mach number. The influence of the compressibility of the air in this range of speeds is negligible, so that, if the Reynolds number of the model in the wind tunnel is the same as that of the full-scale body in free-flight conditions, a difference in velocities is not very important.

(2.2) High-Subsonic-Speed Tunnels

For subsonic-speed tunnels the speed of the air stream is usually controlled by varying the speed of the compressor, for which a wide range of operating speeds and accurate control of speed are required. Two typical sets of conditions would be a 10 : 1 speed range with an accuracy of 0·25% of maximum r.p.m. or 5 : 1 speed range with an accuracy of 0·1% of set speed (r.p.m.). For such applications, the Ward Leonard drive with electronic or magnetic-amplifier control is ideal, and generally such drives present no special difficulties.

(2.3) Supersonic Tunnels

The speed of the air stream is governed by the shape of the convergent-divergent nozzle which accelerates the air upstream of the model. Changes in speed are effected by changing the shape of the nozzle, the speed of the compressor remaining unchanged. Some control of compressor speed is desirable, however, in order to obtain better matching of the compressor and tunnel characteristics and thereby to save power. When it is realized that the power required to drive a supersonic tunnel is of the order of 1 000–3 000 h.p. per square foot of working section, and that in the 8 ft × 8 ft high-speed tunnel at the Royal Aircraft Establishment, Bedford, the total driving power is 80 000 h.p., it will be appreciated that, if better matching gives a saving in power of only 1 or 2% of this figure, there will be an appreciable saving in running costs.

The power requirements of drives for supersonic wind tunnels can usually be met by a slip-ring induction motor with rotor control to give 15–20% reduction in speed, with an accuracy of speed control of about 0·25% of r.p.m.

(2.4) Transonic Tunnels

Accurate control of speed over a small speed range is required for transonic tunnels. A composite drive of a.c. slip-ring induction motor coupled to a d.c. pony motor would be a suitable driving combination for a large transonic tunnel to provide a speed range down to 90% of maximum r.p.m. and an accuracy of control of 0·1% of r.p.m.

(2.5) Multi-Purpose High-Speed Tunnels

There are obvious advantages to be gained from combining two or even all three of these basic types of high-speed tunnel, i.e. high subsonic, supersonic and transonic into one tunnel. There is the saving in the cost of the tunnel itself compared with the cost of two or three single-purpose tunnels. There is also the saving in the time taken to carry out the full range of tests on models of aircraft designed for supersonic speeds as well as the advantage of being able to compare directly the results of all the tests.

Offsetting these advantages in such a combined, or multi-purpose, high-speed tunnel is the difficulty of providing a drive and control system capable of meeting the operating conditions of high power, wide speed range and accurate speed holding throughout the range.

(3) REVIEW OF WIND-TUNNEL DRIVES

(3.1) General

Most wind-tunnel fans, or compressors as the larger multi-stage air impellers are better termed, are driven by electric motors, since the reliability, cleanliness, range and precision of control and general convenience in working make this method ideal from an operational point of view.

Where very large powers are concerned, however, electric drives are likely to be subject to tight restrictions imposed by operating schedules, which limit power and working hours and which are designed to keep the site maximum demand, and hence maximum-demand charges, within a given figure. This leads to a consideration of direct drive by prime movers or of site generation of electrical supply as an alternative to, or used in conjunction with, the public supply.

The choice of drive is influenced also by the required power range, the speed range and the capital cost of the plant.

A review of the field of existing and projected wind-tunnel drives indicates a general trend towards combinations of a.c. synchronous and induction-motor drives for the large special-purpose tunnels, but with the Ward Leonard system paramount for the smaller subsonic tunnels, where wide speed range and precise control of speed are necessary. There is a tendency to rate motors for continuous duty, and, except in the case of very small tunnels, to mount the drive outside the tunnel with a shaft drive to the fan or compressor in the closed air circuit.

Use can be made of a variable-pitch fan to vary the air speed, and this gives much quicker control than varying the drive r.p.m., but the complications involved in applying pitch change to the blades of a multi-stage compressor rule out this method for any but the simpler types of fan. A synchronous motor is generally used and the blades put into flat pitch for starting, thus leading to a more efficient use of the motor power as limitations in pull-in torque are avoided.

(3.2) Ward Leonard Drive

The Ward Leonard drive is ideal for wind-tunnel duty as precise speed control is easily obtainable over the full range of r.p.m. at a high efficiency. High torque at low speeds can be covered, as this form of drive is one of constant torque over the whole speed range from full speed to the limits of stability of the particular system at low speed.

Tunnels working at atmospheric pressure in the subsonic region require comparatively little power and are ideally powered by the Ward Leonard system on account of the ease of speed control and the large range of speed. In these tunnels the parameter $\frac{1}{2}\rho v^2$ must be kept constant by control of drive speed, and changes of pressure of the order of 0·001 mm Hg can be detected by the measuring apparatus. With this system, automatic speed control can be introduced comparatively easily to hold the speed within limits which are now required to be better than 0·1% of top speed.

This form of drive is also useful for small high-speed tunnels and other research facilities where wide speed range and flexibility of control are an important consideration.

The capital cost is very high, however, being about four to five times that of an induction-motor drive of equivalent power. There is an upper limit of about 20 000 h.p. above which the machines become difficult to arrange, as multi-unit systems have to be used. Prevention of instability between the sets is a problem.

A typical Ward Leonard drive has been installed in the 13 ft × 9 ft low-speed wind tunnel at the R.A.E., Bedford. As shown diagrammatically in Fig. 1, a 1 500 h.p. d.c. motor is mounted outside the concrete tunnel shell and a shaft drive is

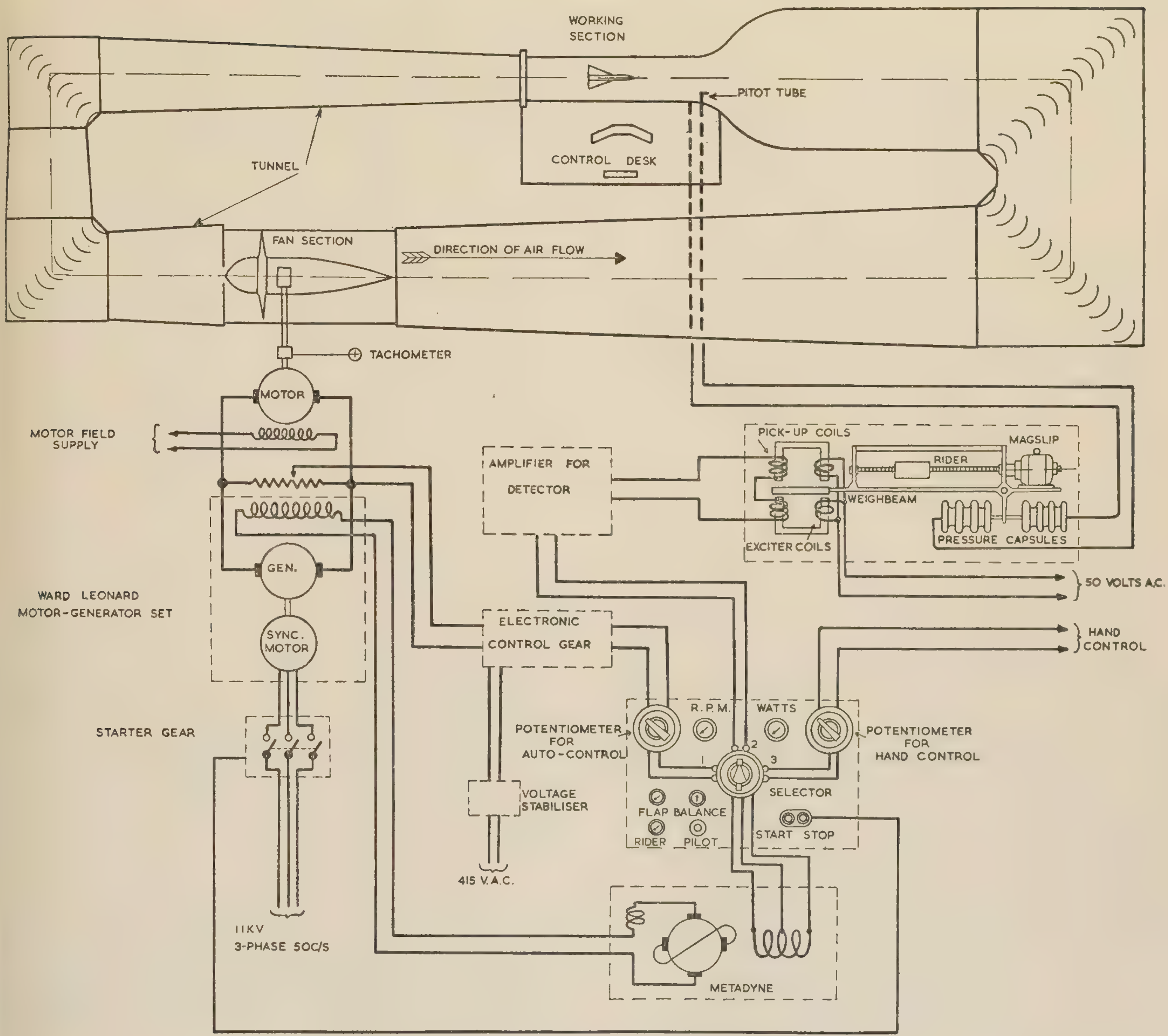

Fig. 1.—Ward Leonard drive and speed-control system.

13 ft × 9 ft low-speed wind tunnel, R.A.E., Bedford.

coupled to a gear-box in the fan nacelle, the fan being a large-diameter single-stage type with fixed blades. A Ward Leonard set supplies the variable voltage to enable the drive motor to cover a speed range of 1 100–135 r.p.m. Control of speed by regulation of generator excitation is effected through a metadyne system which can be arranged to respond to manual control, to signals from speed error-detecting equipment, or to the output of a sensitive pressure balance, according to the control parameter required.

In the high-speed laboratory of the same establishment another drive of this type is used for a 4 000 h.p. centrifugal compressor which provides a common air supply for a group of four test bays in which small supersonic tunnels are rigged as required. The Ward Leonard generator excitation is taken from a pilot exciter, and the controls for this exciter are mounted on a small wheeled cubicle which can be moved to the test bay in use and plugged into the control system to provide local control of compressor speed, as shown diagrammatically in Fig. 2.

(3.3) D.C. Drive with Rectifier

The advantages of a d.c. variable-speed drive can be secured by feeding a d.c. motor from a grid-controlled mercury-arc rectifier connected to the a.c. supply. By controlling the firing point of the anodes precise speed control can be achieved over the full range of r.p.m. with reasonably high efficiency.

Disadvantages of this system are poor power factor on low loads and the absence of any form of power-factor control. The cost is high compared with that of an equivalent induction-motor drive, and the power limit of a single unit is about 6 MW.

An interesting example of this type of drive is now in commission on the 3 ft × 3 ft high-speed tunnel at R.A.E., Bedford,[2] where two d.c. motors drive a two-stage centrifugal compressor system.

Two grid-controlled water-cooled steel-tank mercury-arc rectifiers provide the d.c. supply for the motors and are each rated at 750 volts, 6 380 amp maximum output.

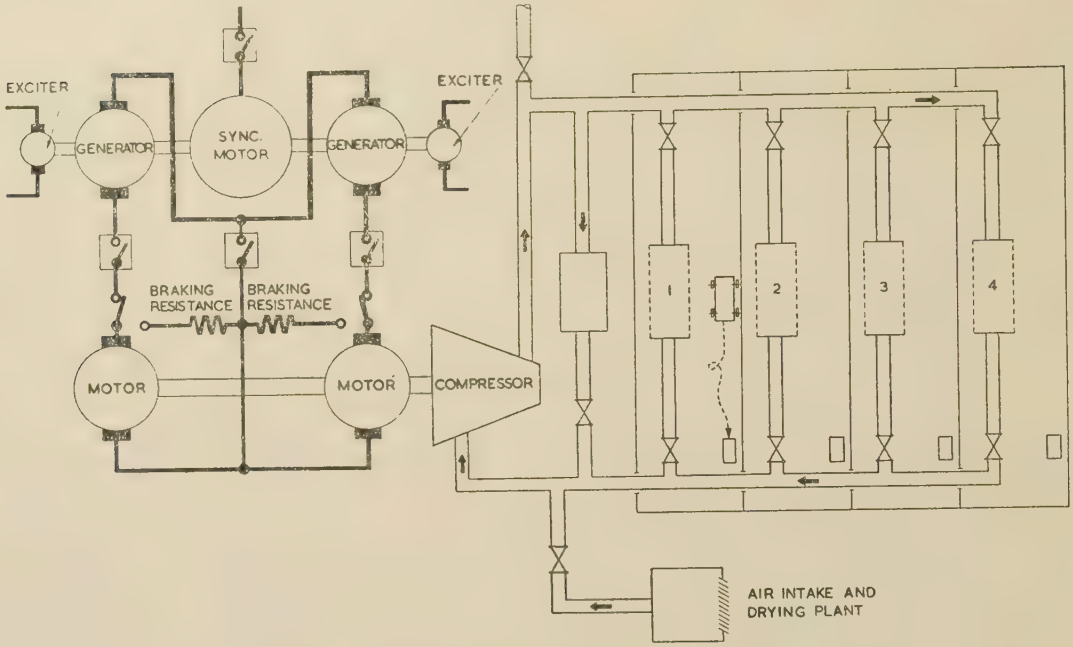

Fig. 2.—Ward Leonard drive with four control points.
High-speed laboratory, R.A.E., Bedford.

Control is effected by superimposing an alternating voltage on constant negative potential applied to the rectifier grids. Positive peak impulses are introduced into the alternating voltage by means of saturated transformers, and the time of anode firing is varied by altering the phase of the impulse in relation to that of the main a.c. supply by means of pilot-motor-driven phase-shifters. Speed control of the drive is thus carried out by manual or automatic control of the phase-shifter pilot motors. A simplified diagram of this system is shown in Fig. 3.

A similar type of drive of the order of 1 000 h.p. has been in use for some years on the 9 in supersonic tunnel at R.A.E., Farnborough. In this case, additional fine control of the grid voltage has been achieved by the use of chokes directly fed from the control-parameter signal.

(3.4) Induction-Motor Drive

The a.c. wound-rotor induction motor with rotor-resistance control provides the most economical drive where only a narrow speed range is required. Speed control of the order of 0·25% of set speed can be achieved over a range of 80–100% r.p.m. on specially designed machines if attention is given to controlling the rotor-resistance electrolyte temperature.[3]

This form of drive is particularly suitable for special-purpose supersonic and transonic wind tunnels where the speed range is small. Braking of such a drive is now generally carried out by injecting direct current into the stator system.

Efficiency and power factor at the reduced speed are very poor, however, particularly on 'non-cube-law' drives. There is otherwise no limit of application, as a drive of very large power can be installed by coupling a number of motors to the drive shaft.

One of the problems encountered in the design of large wind-tunnel drives is that of torsional vibrations. The shaft system is usually a long one with multiple supports, and it is found difficult to prevent one of the various torsional modes being excited at or near the operating speed.

With an induction-motor drive, in particular, unbalanced impedances in the 3-phase rotor circuits will cause the machine to develop an oscillatory torque component having a frequency of twice the slip frequency and an amplitude dependent on the degree of rotor-current unbalance and the motor loading.[4]

It is essential, therefore, to ensure that unbalance of both rotor windings and rotor regulating resistances is kept within very close limits and that the design of the shaft system confines any remaining torsional oscillations to the low-power low-speed region.

(3.5) Induction Motor with Slip Control

For very large powers, where efficiency at part load is of importance and a wide speed range is required, the system usually known as 'modified Kramer'[5] can be employed with a normal wound-rotor induction motor, as shown in Fig. 4. The rotor is connected to an a.c. synchronous motor coupled mechanically to a d.c. generator, and this set is arranged to run at variable speed corresponding to slip frequency.

The output of the d.c. generator is then connected to a d.c. motor driving an alternator at constant speed, the output of the alternator being connected to the main a.c. supply. The slip energy of the rotor is thus fed back to the line and gives a constant-torque characteristic to the drive. In the case of the straight Kramer system the slip energy is applied to the main motor shaft and the drive has a constant horse-power characteristic.

An effective speed range of 10 : 1 is possible, and automatic speed control of the order of 0·25% of set speed can be conveniently applied by means of electronic control of excitation on both d.c. machines.

For a fan drive with air at constant density, i.e. with torque proportional to the square of speed, the maximum power handled by the auxiliary sets is small compared with that of the main motor, and as the constant-speed set can be run at a high economical speed, the size and cost of this unit will be quite small. The variable-frequency set, however, will have to handle the developed slip power at a low speed so that this set will be relatively larger than the constant-speed set.

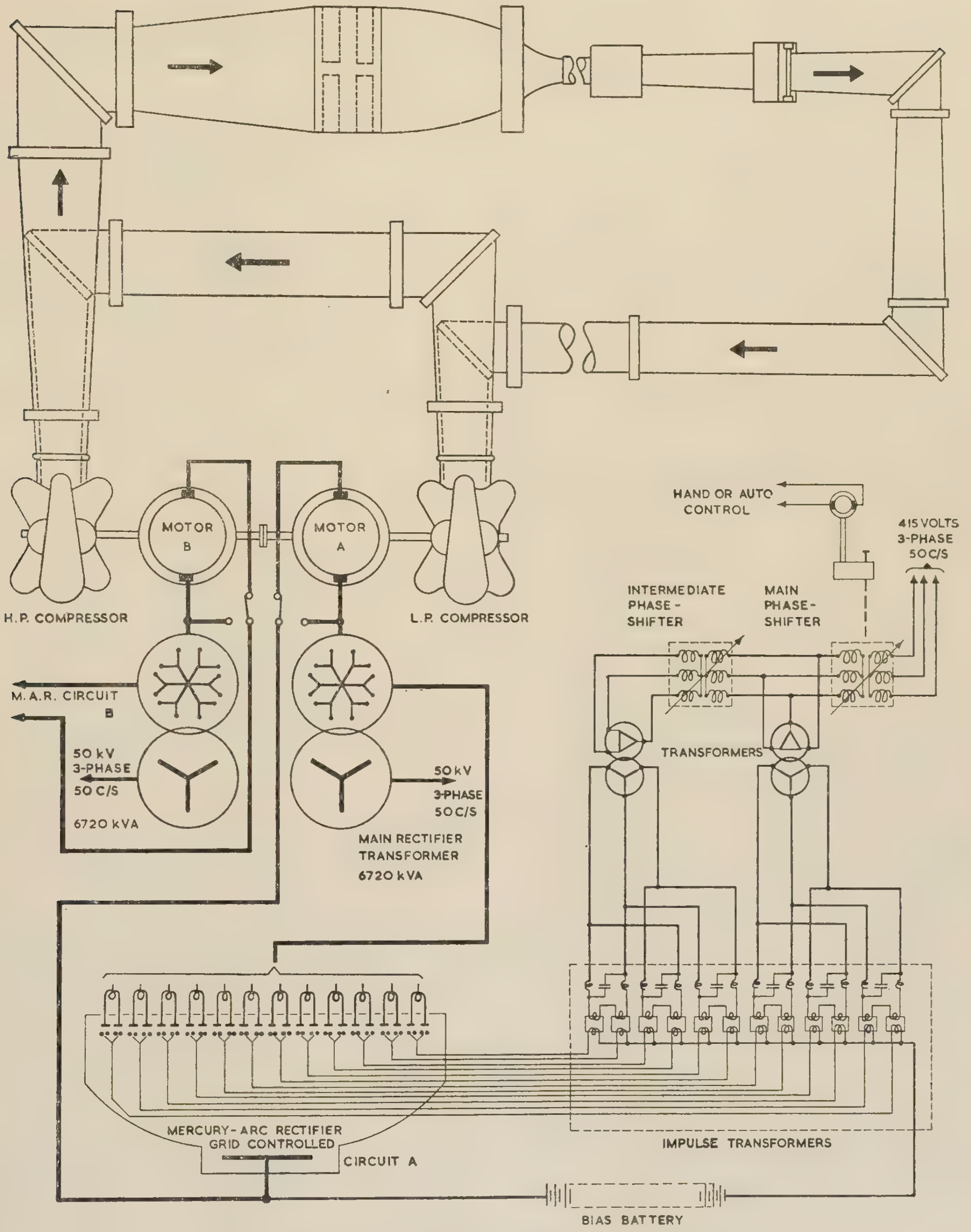

Fig. 3.—Schematic of d.c. drive with grid-controlled rectifiers.

3 ft × 3 ft supersonic wind tunnel, R.A.E., Bedford.

If the requirement is for a drive with constant torque at all speeds, such as would be the case with a variable-density tunnel drive, the maximum slip energy would be much larger, and as the size of the auxiliary sets might well approach that of the main motor, the system would have no advantage over the ordinary Ward Leonard drive.

The use of the Kramer drive is almost entirely confined to the United States, where it has been widely used on wind-tunnel drives,[6] one of the largest being that of the Wright Field 10 ft supersonic wind tunnel[7] with a drive of 40 000 h.p.

(3.6) Composite Drive

An alternative to the Kramer system has been developed for wind-tunnel duty in which a d.c. motor with Ward Leonard control is coupled in tandem with the a.c. induction motor. Connected to the rotor slip rings is a liquid regulating resistance having special arrangements for controlling the temperature of the electrolyte, which is circulated by a pump.

A combined system of electronic control is arranged for both a.c. rotor regulator and the Ward Leonard system, and sharing of the load occurs automatically throughout the speed range.

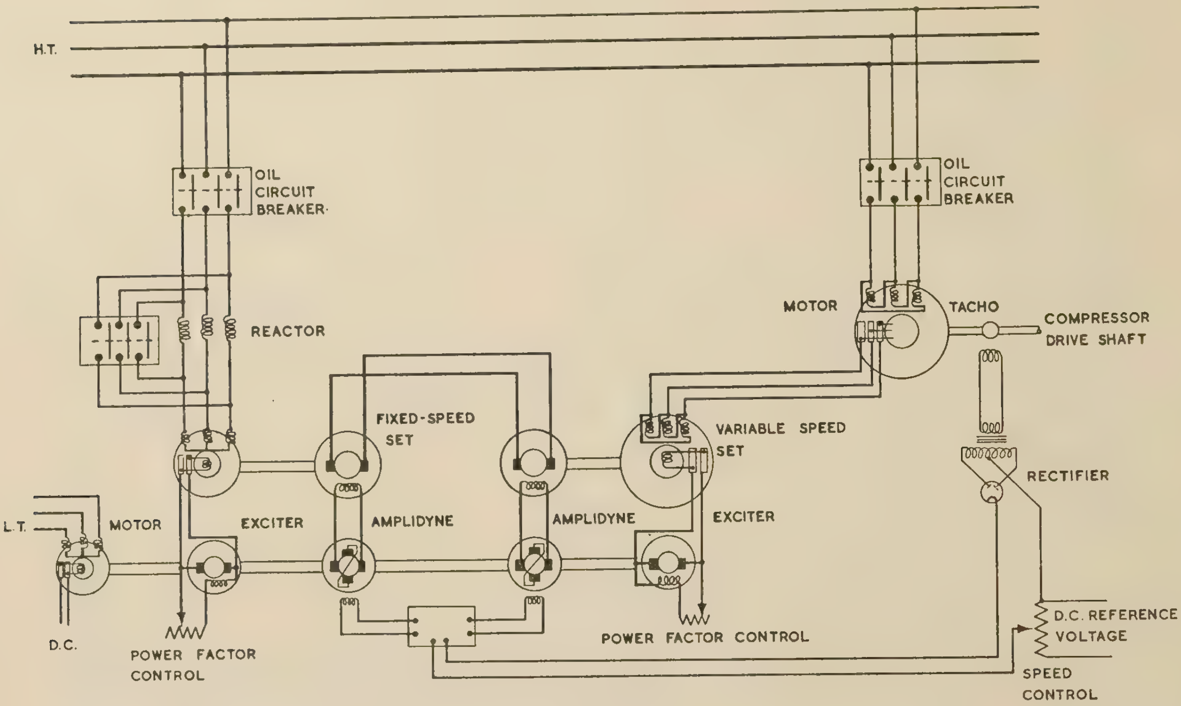

Fig. 4.—Diagram of modified Kramer drive system.

The d.c. motor is used throughout the speed and power range for vernier control of speed and may well provide all the power up to about half speed, above which the a.c. motor is switched in. Full-load high-speed efficiency is slightly better than with the Kramer equipment, but below half speed the efficiency is lower. If, however, the drive is associated with a variable-pitch fan, the power at maximum speed may vary over a ratio of 2·5 : 1 at nearly constant slip, and then this method shows an advantage over the more complicated Kramer system. The capital expenditure is generally very little less, however, on account of the expense of the large rotor regulator, pump and heat exchanger.

A transonic tunnel with a large drive of this type has been built in this country by the Aircraft Research Association. In this tunnel the two-stage axial fan is driven by a 25 000 h.p. slip-ring induction motor to which is directly coupled a 1 500 h.p. d.c. motor. The speed of the induction motor is controlled through a bank of liquid resistors and the d.c. motor is supplied by a Ward Leonard generator driven by a 1 770 h.p. synchronous motor. Speed control to limits of 0·2% of set speed is achieved over the range 350–485 r.p.m. by closed-loop servo control from a tacho-generator driven from the main shaft.[8]

(3.7) Synchronous Drives

Synchronous-motor drives are in use on many types of low-speed wind tunnels, but except for special-purpose applications, it is necessary to obtain a range of air speed by the use of variable-pitch fan blades which are arranged to be either adjustable when the fan is stationary or continuously variable by hydraulic or electrical servo control.

For supersonic working, however, where a ready means of changing the Mach number is available, a synchronous motor working at fixed frequency from the Grid system would provide a suitable drive for tunnel compressors of the largest powers at present contemplated.

A tunnel with a drive of this nature could certainly cover a wide range of supersonic working, but from a study of the performance requirements outlined in Section 2, it is clear that the limitation of fixed-speed working would restrict the usefulness of such a tunnel. Serious consideration must be given, therefore, to providing variable speed in order to extend the range of operation of the tunnel and to use more efficiently the high capital outlay of this type of installation.

Such speed variation can only be obtained by providing a variable-frequency supply for the main synchronous motor, either by a frequency-convertor system or by local generation. With the frequency-convertor system the main motor is fed from the output of an induction-type convertor in which the rotor is coupled to a d.c. motor with Ward Leonard speed control. This system is comparable in installed capacity to the Kramer system, requiring plant of about 2·5 times the maximum shaft power with similar facilities for power-factor correction, and is even more amenable to automatic speed control. However, with the absence of rotor slip energy to be dissipated or fed back to the supply, the system should show considerable advantage at lower speeds.

Ideally, the synchronous motor can be fed from a locally-generated variable-frequency supply, with the frequency under the control of the tunnel operator for variation of air speed as required.

For starting the compressor from rest and running the drive up to synchronous speed an auxiliary driving motor of considerable power is required, even if the compressor is unloaded by evacuating the tunnel. This auxiliary drive would require to be a variable-speed type such as a d.c. motor with Ward Leonard speed control or a wound-rotor induction motor with slip regulator. In addition to running up the drive the auxiliary motor could well be used for speed-control purposes in conjunction with the variable-frequency supply.

The vertical spinning tunnel at R.A.E., Bedford, which provides an example of the synchronous drive in a low-speed tunnel, has a large single-stage fan for circulating air through a vertical working section which forms part of the closed air circuit, as shown in Fig. 5. A vertically-mounted synchronous motor of

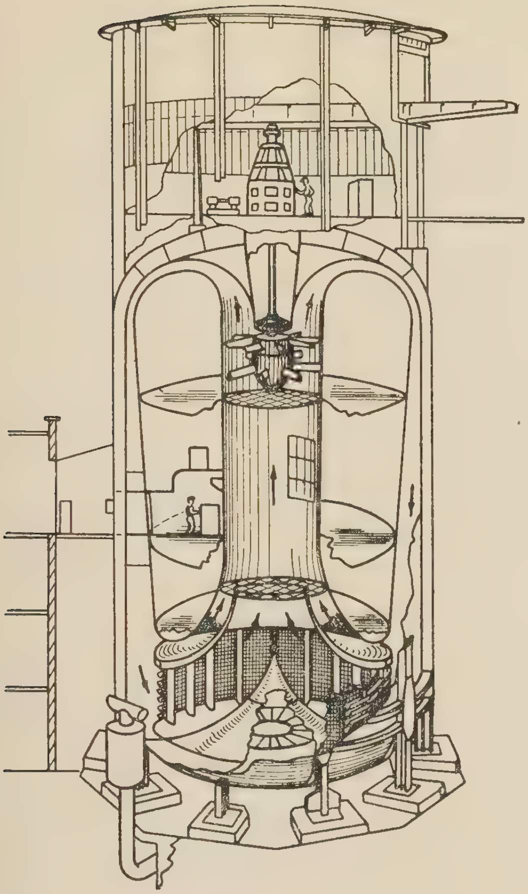

Fig. 5.—Synchronous-motor drive with pitch changing of blades.
Vertical spinning tunnel, R.A.E., Bedford.

2000 h.p. continuous rating drives the fan shaft at a constant speed of 500 r.p.m., and air speed is adjusted by means of hydraulic pitch-changing mechanism which actuates the variable-pitch fan blades by means of a push-pull rod passing through the hollow shaft of the motor. The motor is started and run up to synchronous speed with the fan blades in fine pitch by means of a separate starting winding controlled by a liquid starter, and synchronizing is carried out automatically. The pitch-change control lever, together with start-stop pushbuttons and a dynamic-braking pushbutton, are thus the only controls necessary to operate the drive, and these are arranged for switching to each of three control positions as required.

An example of the synchronous-motor/induction-motor combination of drive units is being installed in the high-supersonic-speed wind tunnel now being built at R.A.E., Bedford. Air is circulated through the working section of this tunnel by two sets of compressors, each comprising an axial-flow l.p. compressor and a centrifugal h.p. compressor with a common driving shaft coupled to a 2-pole turbo-type synchronous motor of 34 000 h.p. Also coupled to each shaft through gearing is a 10 000 h.p. 6-pole wound-rotor induction motor controlled by a liquid regulator having a maximum continuous rating for 28% slip.

Each set is started and run up to synchronous speed at a reduced air density by means of the induction motor, and when the main motor has been synchronized to the supply, the load on the induction motor is reduced to a value within its rating by means of the liquid regulator.

As shown in Fig. 6, the synchronous motor can be connected either to an 11 kV Grid supply or to a locally-generated variable-frequency supply, but the induction motors are supplied only from the Grid. When the synchronous motors are supplied at variable frequency the drive-speed range is 72–100% with the induction motors running under constant-current/constant-torque conditions to a maximum of 28% slip.

(3.8) Drive by Prime Mover

(3.8.1) General.

Direct drive by means of a prime mover has the advantage of providing complete control of the drive power for running at will over the full range available. Nevertheless, limitations do exist in the form of reliability and regulation of a comparatively small power source, and in this respect a drive of this type cannot compare with one of similar power connected to the very large capacity of the Grid.

Some early tunnels were powered by aircraft-type internal-combustion engines, and it was found that under such conditions the life of these engines was very limited and that reliability was of a low order.

Latterly some use has been made of aircraft turbo-jet engines to draw air through small open-circuit tunnels on the induction principle, as for example in a transonic tunnel described in Reference 9.

The large power requirements of this type of drive, however, make the consideration of local power plant desirable, and, as reciprocating engines are ruled out on account of limitations in size and uniformity of torque, the choice lies between water, steam and gas turbines.

(3.8.2) Water-Turbine Drive.

A natural supply of water with sufficient flow and head forms a very convenient source of power for a large wind tunnel in those countries favoured with the necessary geographical features.

Difficulties arise in braking and slow running, but effective control of drive speed and air speed can be arranged by remote control of sluices and by variable-pitch impeller blades.

The range of air speed is consequently large and the efficiency is high, while the installation cost is lower than any comparable form of drive.

The siting of an actual tunnel is usually difficult owing to the mountainous terrain encountered, and the site chosen may well depend upon the economics of piping the water to a place of reasonable access for the plant and personnel required.

An interesting example of this type of drive is the French wind tunnel at Modane-Avrieux, where a total drive power of 120 000 h.p. is developed by water diverted from the Aussois hydro-electric power plant of Électricité de France on the River Arc. In this drive two 14 ft-diameter Pelton turbine wheels are separately housed, one on each side of the tunnel, and connected by shafting to contra-rotating propeller blowers, each of which develops 54 247 h.p. at 250 r.p.m. under an actual head of 2 690 ft.

Air speed is controlled by a variable-flow water valve on each turbine together with auxiliary valves for maintaining constant flow under light-load conditions. The air-speed range is extended by adjustment of the pitch angle on the propeller blades. It is claimed that the governing system maintains the turbine speed constant to within $\pm 0{\cdot}05\%$ of the nominal speed. Braking is carried out by means of a counterflow system on the turbines which reduces considerably the time required for bringing the tunnel air flow to rest.[10]

(3.8.3) Steam-Turbine Drive.

Direct drive by steam turbine appears to present many advantages. Steam-turbine design is well established; sets are reliable

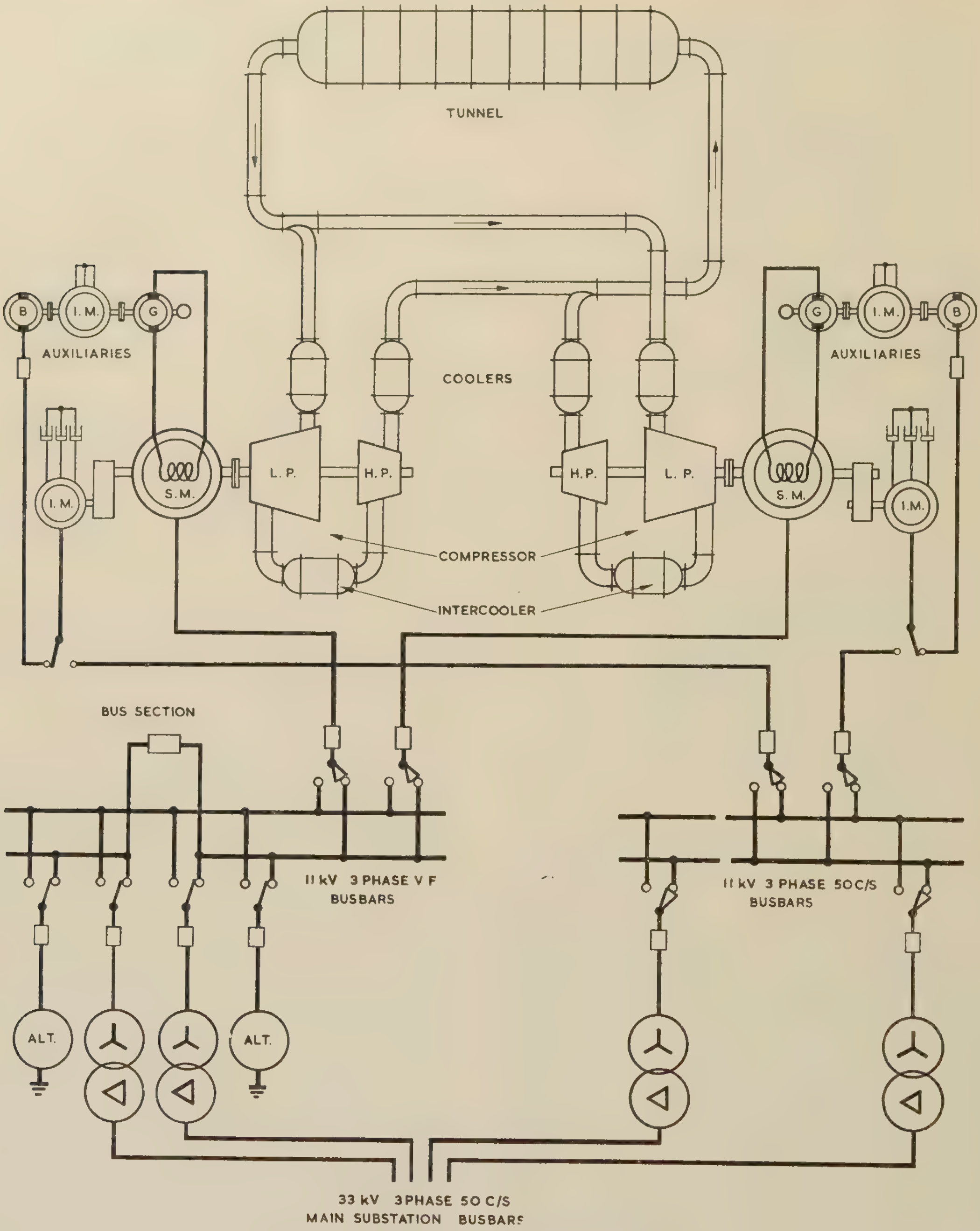

Fig. 6.—Schematic of compressor drives.
High-supersonic-speed wind tunnel, R.A.E., Bedford.
L.P.: Low pressure.
H.P.: High pressure.
I.M.: Induction motor.
S.M.: Synchronous motor.
G : Excitation generator.
B : Braking generator.

and their performance is well proved, torque is smooth and the variable-speed feature would not present any problem in control. There are, however, several disadvantages in using such a drive in an establishment of the kind under consideration. For an independent tunnel, the standby steaming duty of the boiler would be most uneconomical since the load factor of a single drive might well be only of the order of 10%.

It might be a better proposition at an establishment having a large steam capacity for other purposes, such as a base heating load, but the use of such steam would most likely be confined to turbine drives for small special-purpose tunnels with considerable limitation of load scheduling.

Many of the above disadvantages could be overcome by coupling the turbine to an alternator to provide a source of variable-frequency electrical power. While the cost of the alternator and associated switchgear would be high, the actual tunnel drive would be considerably less costly than a frequency convertor system. Nevertheless local generation could not be

justified unless the load factor was very much higher, which, for wind-tunnel duty, would imply load dispatching over a number of drives.

(3.8.4) **Gas-Turbine Drive.**

The use of a gas turbine as a source of power would avoid some of the limitations put forward in the case of steam.

It would be much more economical in space and require fewer operating staff—important considerations in a research establishment. It could meet the demands for full load more quickly, and starting up from cold could be done in under an hour with rapid increase to full power thereafter. When not required the plant could be shut down immediately, and, apart from a few hours barring for cooling purposes, would require no standby duty.

Since large tunnel drives of the type now being considsred require continuous control of speed over a large range and probably high-torque low-speed working as well, the use of a simple direct-coupled turbine system would not cover these requirements. Also the limitation of useful power per unit to about 10 000 h.p. imposed by having the turbine air compressor on the same shaft, would lead to difficulties of a multi-pinion drive shaft to allow a number of units to be coupled in order to build up the total shaft power required.

Greater flexibility would be obtained by the use of compound sets with separate power turbines which would be able to run over a greater range of speed and would be capable of achieving full-load torque down to about 25% full speed. Difficulties would still exist with mechanically coupling more than one set to the drive shaft, but the individual turbines would develop about twice the useful power of the previous case, 27 000 h.p. being the apparent limit.[11, 12]

As in the case of steam plant these difficulties of direct coupling can be overcome by introducing an electrical link and installing a turbo-electric drive system.

(4) SITE GENERATION OF POWER AT VARIABLE FREQUENCY

The previous Sections have outlined the background against which the power requirements of an aircraft research establishment were studied when a number of large wind tunnels had to be catered for over an extended programme of development.

The establishment's load falls into three categories:

(*a*) *Base Load.*—Comprising low-power tunnels and other research facilities, workshops, auxiliaries for all tunnels and services for office accommodation and for tunnel buildings.

(*b*) *Medium-Power Loads.*—Comprising low-speed tunnels and small supersonic tunnels.

(*c*) *High-Power Loads.*—Comprising large supersonic and multi-purpose tunnels of 50 000–100 000 h.p. and upwards.

The first two requirements can be met quite readily by the normal C.E.A. supply with conversion by Ward Leonard sets, or rectifier units, for variable-speed working. It is the third group which presents the problem of variable-speed high-power working, since both types of tunnel require some adjustment of speed, the multi-purpose tunnel demanding a speed range of the order of 5 : 1 with an accuracy of speed holding of 0·1% of set speed, while the purely supersonic tunnel can function with a shorter range of about 25% from top speed, but with similar speed-holding accuracy.

From the examination of the electrical driving systems in Section 3 it is clear that, if the establishment has only a fixed-frequency supply from the C.E.A., only the Ward Leonard and frequency-convertor systems merit consideration for these drives.

A drive of 50 000–100 000 h.p. is well beyond the normal range of a single-unit Ward Leonard system, and if the drive is divided into a number of motor-generator sets each limited to about 10 000 h.p., the resulting arrangement is expensive in first cost, unwieldy and complicated to control, and since the variable-speed feature is vested in the high-current variable-voltage d.c. busbars, the supply is difficult and expensive to extend to other tunnel drives.

The same general remarks apply to the use of a frequency-changing system, but there would be the additional disadvantage of uneconomic working at the lower frequencies. The application of the output of this system to other wind-tunnel drives would be quite practicable, however, as the transmission would be by variable-frequency alternating current.

The difficulties of conversion from fixed frequency to variable speed draw attention to a third possibility, that of site generation of electrical power. The technical advantages are great and the capital and running costs compare favourably with any possible alternative.

If generation is effected by several turbo-alternators capable of running independently at variable speed, flexibility of distribution between the various tunnels can be achieved by a sectionalized busbar system to allow individual tunnels to be run simultaneously at different frequencies. This arrangement presupposes that the control systems of both tunnel drive and prime movers are so arranged that the drive speed can be adjusted from the tunnel control room by remote control of prime-mover speed.

Where the normal load demand of the establishment is high and a C.E.A. supply of similar capacity to that generated is available, this supply might well be arranged for switching to the large tunnel drives as an alternative to the variable-frequency supply. The Grid supply could then be used during periods when only full-speed running is required, or for supplying other tunnels connected to the system when the generated supply is in use for tunnels requiring variable-speed working. This latter case would entail working outside the peak-load periods of the establishment and most likely would be at night or by special arrangement of load scheduling.

The generating station could, of course, be used for peak-load working at standard frequency by arrangement with the supply undertaking.

Such an installation could be arranged most conveniently with the generating-station control room acting as the load dispatching centre for the group of tunnels requiring variable-frequency or Grid supplies for main drive power. The normal functions of this control room, which would be working to a pre-arranged schedule of load requirements, would be to start and synchronize the generators to specified busbar sections; to connect the Grid-supply interconnectors to other busbar sections as required, and to co-ordinate the synchronizing of tunnel driving motors including the final closure of the circuit-breakers.

The main driving motor of each tunnel would be a synchronous motor with an auxiliary motor to run up the drive to synchronizing speed and to provide additional power or speed-holding facilities as discussed in Section 3.7. It is evident, therefore, that the prime-mover speed-control system must be linked with the speed-control system of the auxiliary drive when the latter remains coupled to the drive after synchronizing, and a very flexible arrangement of control switching is necessary to allow the tunnel operator to secure immediate control of drive speed after synchronizing.

(5) CONCLUSION

From the foregoing considerations, therefore, it is evident that, for an establishment which is to have several large wind tunnels with variable-speed high-power drives, site generation of variable-frequency power, together with the C.E.A. Grid supply, has a

number of important advantages over power systems based on frequency conversion.

Such a system is flexible in operation; it can be readily extended with comparatively little expense; it enables the fullest use to be made of a number of variable-speed high-power drives for a given maximum-demand charge; and also takes advantage of the simplicity and the availability in very large powers of synchronous motors which are used as the main driving units.

(6) ACKNOWLEDGMENTS

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[The discussion on the above paper will be found on page 228.]

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A VARIABLE-FREQUENCY POWER INSTALLATION FOR LARGE WIND-TUNNEL DRIVES

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SUMMARY

At a new Government research establishment, site-generated variable-frequency power is used with synchronous motors to provide variable-speed drives for large wind tunnels. The paper discusses the use of gas turbines as prime movers and describes the special features of the variable-frequency system and its interconnection with the C.E.A. Grid supply.

Finally details are given of the operation of the first large wind tunnel to be connected to the variable-frequency power system.

(1) INTRODUCTION

During the planning of the Royal Aircraft Establishment, Bedford, a survey of the probable power requirements showed that, although the base load and the medium-size projects could be met by a 30 MVA supply, the peak load would be fixed by one, or perhaps two, of a number of very large wind tunnels with drives of the order of 50 000 h.p. and upwards. At that time only the first of these very large projects—the 8 ft × 8 ft high-speed wind tunnel—was in the early design stage, and there was little available information about the others. Nevertheless, the 8 ft × 8 ft high-speed tunnel, which was to be a multi-purpose one with a wide speed/power range, was thought to be the largest single driving unit likely to be built at the Establishment, and, as such, could be used as the basis of the design of the power supply system.

As described elsewhere,[1] for such an Establishment, there are many advantages to be gained from having a site-generated variable-frequency power supply in addition to the C.E.A. Grid supply, the chief being that it provides a neat and flexible variable-speed system when used to supply synchronous driving motors.

Although the primary and predominant reason for site generation is the need for a variable-frequency supply, it was considered that other advantages would accrue, of which the most important are peak lopping and better use of the available power within the maximum demand.

From the outset, therefore, it was clear that the type of prime mover required was something analogous to the oil engine but in a much higher power range, and a study of the possible alternatives suggested that the gas turbine should be seriously considered.

(2) GAS TURBINES AS PRIME MOVERS FOR THE GENERATION OF POWER AT VARIABLE FREQUENCY

For variable-frequency working, it was assessed that a power of 40 MW would meet the operating requirements of the 8 ft tunnel, and to give the required flexibility of power distribution for the future large drives in the Establishment, it was considered that this power could best be provided by two generators, each of 20 MW capacity.

At that time, however, no running experience was available on gas-turbine sets of this size, so that a full investigation was made into the practicability of designing and manufacturing sets of this order. British manufacturers and the National Gas Turbine Establishment were consulted, and a review was made of Continental experience with particular reference to the work of the Swiss manufacturers in this field. Several designs had been put forward for sets up to 10 MW, and a unit of 27 MW had actually been put in hand for Beznau power station. These sets, however, were for generating at constant frequency and in most cases for standby duty, whereas the requirements for the R.A.E. sets were for variable-frequency working over a wide range of load. Indeed, these requirements were different from any yet encountered, and it was seen that a compromise would have to be made between the gas-turbine cycles advocated for constant-speed supply-system generation and those suitable for rapidly-varying speed/load conditions. It was necessary, therefore, in order to assess the advantages of the gas turbine over other prime movers, to determine the best cycle for such duty.

It was estimated that for one wind tunnel of this type the load factor would be about 10% and there would be no advantage to be gained from a complicated cycle offering high efficiency.

Nevertheless, a simple single-shaft compressor turbine cycle would not be satisfactory since the variable-speed/variable-load working would force the air compressor to work away from its design point over a large part of the load range. With the power turbine on a separate shaft from the compressor, however, a drop in load would cause the mass flow of air to be reduced in proportion to the reduction of fuel, thus keeping the air/fuel ratio reasonably constant and allowing the compressor to work under optimum conditions.[2] A further advantage of the free-running turbine would be to assist the control of alternator speed over the working range.

For this size of unit also, division of the compressor into low-pressure and high-pressure stages would be essential, and this would lead to the use of an intercooler between the stages in order to maintain the power output of the sets without a corresponding increase in temperature and stress on the plant. The cooling-water supply for the intercoolers would be no problem since it would be only a fraction of the water required by steam generating plant of equivalent capacity.

One requirement of wind-tunnel duty, which is quoted in favour of the gas turbine, is the quick rise to full load after starting up. This would necessitate special attention being paid to the design of clearances, balanced temperature stresses and adequately cooled rotors.

The design of efficient gas-turbine units of this size entails the use of high maximum temperature, which affects the creep rate of the turbine-blade material. With gas turbines, however, there is a rapid easing of stress as the load drops even slightly below full load. Therefore, turbine life bears due relationship to the load factor, and for the wind-tunnel duty proposed it was evident that a reasonable turbine life could be expected. Further, the heavily-stressed blades are confined to the h.p. turbines and

Messrs. McKearney, Drake and Mallalieu are at the Ministry of Works.

replacement of these every few years as a regular maintenance item would not be very costly.

From the above considerations the preliminary designs of suitable gas-turbine sets were mapped out, development work was undertaken and final designs and manufacture were put in hand.

(3) THE VARIABLE-FREQUENCY SYSTEM

(3.1) Generation

The gas turbines work on an open-cycle system with two stages of compression, with cooling between stages, and two stages of expansion, each set having four independent shaft systems, as shown in Fig. 1.

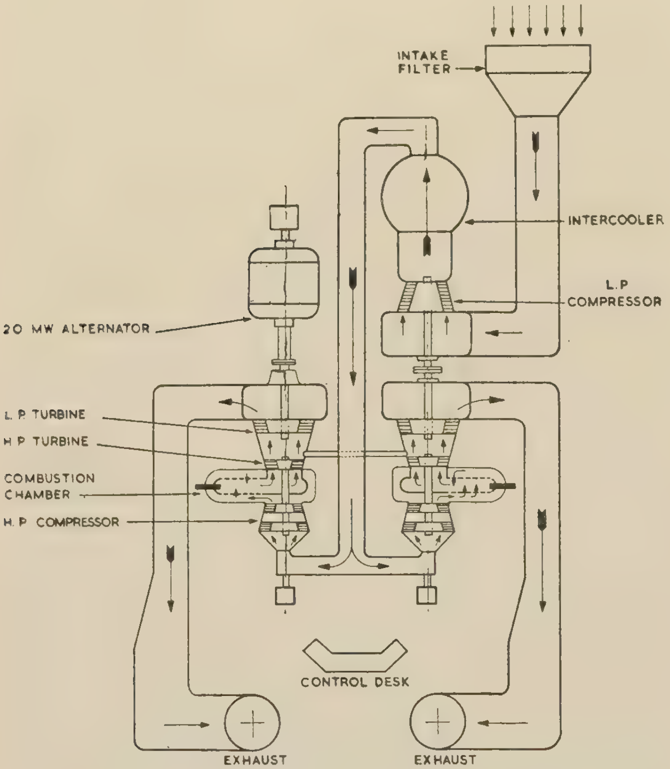

Fig. 1.—Gas-flow diagram for the 20 MW gas turbine.

Each gas turbine has two high-pressure sets consisting of high-pressure compressor, combustion chamber and high-pressure turbine. Of the two low-pressure turbines, one, the charging turbine, drives the low-pressure charging compressor, and the other, the power turbine, drives the alternator.

The fuel is gas oil which is pumped to four atomizing burners in each combustion chamber and injected with blast air at twice the working pressure.

The alternators are 2-pole 3-phase machines which generate 11 kV at 50 c/s. Over the operating range of 10–50 c/s, 600–3 000 r.p.m., voltage is proportional to frequency, i.e. 2·2–11 kV. The alternator exciter sets are separately driven at constant speed by slip-ring induction motors, and slip-ring induction motors also drive the alternator cooling fans for the conventional cooling air circuit.

Although it has been stated that two gas-turbine alternator sets with a total output of 40 MW were required for the 8 ft × 8 ft high-speed tunnel, the synchronous driving motor for this tunnel has to deliver the equivalent of 38 MW at 36 c/s, and thus each alternator has to supply 19 MW at 36 c/s. With the alternator voltage proportional to frequency, its output is also proportional to frequency, and its rating at 50 c/s is 26·4 MW. The gas-turbine power/frequency characteristic is shown in Fig. 2, together with the alternator characteristic. The gas-turbine characteristic is approximately parabolic in shape,

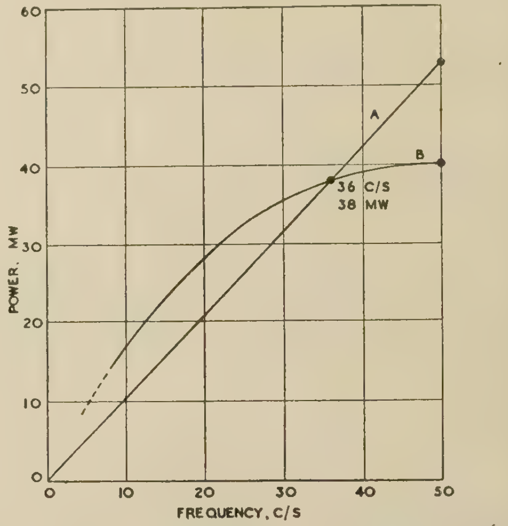

Fig. 2.—The relationship between gas-turbine power and synchronous-machine power for two generating sets.

A. Synchronous-machine capacity.
B. Gas-turbine power.

flattening out in the upper part of the frequency range to give 19 MW at 36 c/s but only 20 MW at 50 c/s.

The alternator voltage is controlled by an automatic voltage regulator which receives its input signal from a voltage transformer in the alternator output circuit. The secondary winding of the voltage transformer is connected to a simple circuit consisting of a resistor, inductor and capacitor in series, and the voltage across the capacitor is fed to the automatic voltage regulator (a.v.r.). This voltage remains substantially constant as long as the alternator voltage is proportional to frequency. If, however, the alternator voltage departs from proportionality, a correcting signal is transmitted to the a.v.r. It is calculated that between 10 and 50 c/s the a.v.r. will maintain this proportionality within the limits of +0% and $-4\frac{1}{2}$%.

Each gas turbine is controlled from a desk at the high-pressure end of the set. The set is started by using the shaft-driven lubricating-oil pumps as hydraulic motors supplied from electrically-driven oil pumps. The hydraulic motors drive the high-pressure sets up to the speed at which the fuel is ignited and the sets become self-driving. During this period the charging compressor and alternator are barred round, and when the fuel is ignited the low-pressure turbines bring the l.p. shafts up to speed to match the h.p. sets. The driver then brings the alternator up to the synchronizing speed of 10 c/s and hands over the control of the set to the attendant in the central control room, who first closes the alternator-field circuit-breaker, then adjusts the excitation to give 2·2 kV and then synchronizes the alternator with the wind-tunnel driving motor. Once the alternator has been synchronized with the motor, control is vested in the wind-tunnel operating staff, but at any time the turbine driver can regain control by reducing the fuel supply to the engine.

The gas turbines can be started from cold and develop full load within an hour. On shutting down, all the four shafts are barred round until the machines have cooled, after which no further attendance is required. In the event of failure of the l.t. supply to the barring gear, two 100 kW self-contained Diesel-alternator sets are provided to supply the power for barring.

The Diesel-alternators can also supply the power for starting one gas turbine at a time until it has reached self-driving speed and until the alternator is generating 11 kV at 50 c/s. Then, through the site distribution system it is possible to connect the power developed by the alternator to its own l.t. distribution board and to parallel this supply with that of the Diesel-alternators.

Although the gas turbines are to be used primarily for variable-frequency power, they can work in parallel with the Grid by arrangement with the C.E.A. They can be used also for an emergency supply at 50 c/s.

As the power turbine of each set is free running, the generator frequency is entirely dependent upon the load and the rate of fuel supply, and it is essential to have effective control of fuel to maintain constant frequency under given load conditions. A needle-type fuel valve is used, and as this is arranged to have a constant-pressure differential across the orifice, fuel flow is dependent on spindle position only. This spindle responds to the lowest setting of a number of actuating controls and protective devices, which either adjust the fuel flow to the load and speed conditions required or shut off the fuel under fault conditions.

One such device is the speed governor, which is normally set just above the maximum working speed to provide overspeed limitation. Other controls include local hand operation and remote servo control arranged for switching to the control rooms of tunnels using the variable-frequency power.

(3.2) Distribution

Each gas-turbine alternator is connected via its own circuit-breaker to a switchboard in the variable-frequency substation, which is the distribution centre for the variable-frequency supply. The Grid supply is also connected to this switchboard by two feeders from the 33 kV board in the site main substation, transformed down to 11 kV by two 25 MVA inter-connecting transformers (see Fig. 3).

The outgoing feeders from the variable-frequency substation switchboard supply the wind-tunnel driving motors with either variable-frequency power or Grid power as required, and by making use of a double busbar with a bus section switch in the lower set of busbars three independent loads can be supplied simultaneously.

Each circuit-breaker is oil filled and rated at 1 000 MVA breaking capacity at 11 kV. The busbars are compound filled, insulated for 11 kV and have a capacity of 3 000 amp.

The link between the Grid supply to the 33 kV busbars in the site main substation and the variable-frequency substation switchboard enables the gas-turbine alternators to be connected in parallel with the Grid supply by arrangement with the supply undertaking for peak load working. In the alternative arrangement when the gas-turbine alternators are used to reduce the site peak load by supplying part of the power independently of the Grid supply, the connections are also made via the variable-frequency substation and the 33 kV and 11 kV switchboards in the site main substation, but the 33 kV part of the network is then earthed via a 3-phase neutral earthing transformer on the 33 kV side of one of the 25 MVA 33/11 kV inter-connecting transformers.

(3.3) Protection

The protection of a high-power variable-frequency system presents two problems. In a system which operates down to 10 c/s the first problem is to choose the rating of the circuit-breakers to be used. The second is to select the types of protective relays.

Under the present conditions in which two 20 MW alternators supply the 68 000 h.p. driving motor for the 8 ft × 8 ft high-speed tunnel, the maximum fault which a circuit-breaker has to clear is 680 MVA by the alternator circuit-breaker in the event of a 3-phase short-circuit occurring at the alternator terminals. If the system is extended to include a third 20 MW alternator for which space in the generating station has been provided, the maximum fault will be increased to 940 MVA.

The circuit-breakers chosen have a breaking capacity of 1 000 MVA at 11 kV, 50 c/s. They have a breaking time from trip impulse to arc extinction on a 100% symmetrical fault current of 0·067 sec, i.e. 3·35 cycles at 50 c/s. The maximum short-circuit current rating is 52 kA.

With voltage proportional to frequency, the maximum fault MVA is reduced from 680 MVA at 11 kV, 50 c/s to 136 MVA at 2·2 kV, 10 c/s, and with the future third alternator connected to the motor the reduction would be from 940 to 188 MVA.

There appears to be little if any information available from which to calculate the breaking capacity of the circuit-breakers at the lower frequencies. Attempts have been made in the United States and elsewhere to forecast the circuit-breaker performance at lower frequencies from a knowledge of the performance at 50 or 60 c/s. For example, at 25 c/s the maximum current rating of the circuit-breaker at 11 kV would be reduced by a factor of 0·7 below the capacity at 50 c/s. Thus the 1 000 MVA rating at 11 kV, 50 c/s would be reduced to 700 MVA at 11 kV, 25 c/s. Therefore, the rating at 5·5 kV, 25 c/s would be 350 MVA, and 140MVA at 2·2 kV, 25 c/s. A further derating factor would then be used to obtain the rating at 2·2 kV, 10 c/s.

On this assessment the circuit-breakers are liberally rated at 11 kV, 50 c/s, but possibly on their limit at 2·2 kV, 10 c/s. To alleviate the duty of the circuit-breakers at the lower frequencies, the tripping circuits are arranged so that, in the event of a fault, tripping of the circuit-breaker is followed closely (0·01 sec) by tripping of the other circuit-breakers in the same circuit.

The alternators have been provided with over-current, circulating-current, standby earth-fault and field-failure protection.

The over-current protection is provided by a triple-pole attracted-armature relay which has the normal range (50–200% f.l.c.) of settings. This instantaneously-acting relay is combined with an auxiliary time-delay relay which is preset but adjustable between 0 and 5 sec.

The circulating current protection is a simple Merz–Price system using triple-pole attracted-armature-type relays, instantaneous in operation. For earth faults this system protects 94·5% of the winding at 50 c/s and 77·5% of the winding at 10 c/s.

The standby earth-fault protection is provided by a single-pole attracted-armature-type relay adjustable over the normal 10–40% range, and instantaneous in operation. It is combined with an auxiliary time-delay relay, preset but adjustable over the range 0–5 sec. The alternator neutral is earthed through a liquid resistor which limits the maximum earth-fault current to 100% full-load current at 11 kV. On the 10% setting the standby earth-fault relay provides protection for 90% of the windings at 50 c/s and 58% at 10 c/s.

Owing to the difference between the gas-turbine and alternator load/frequency characteristic, as shown in Fig. 2, it is possible to overload the turbines within the frequency range 36–50 c/s where the turbine shaft power output is nearly constant. The normal circuit over-current relay mentioned above is insensitive to frequency and responds to current only, thus allowing the alternator to supply a load proportional to frequency.

To protect the turbines over this range an induction-type trip relay having an inverse definite minimum time-lag characteristic has been installed in the alternator output circuit, and, as this relay is inherently sensitive to frequency, the load current has to be reduced as the frequency increases from 36 to 50c/s in order

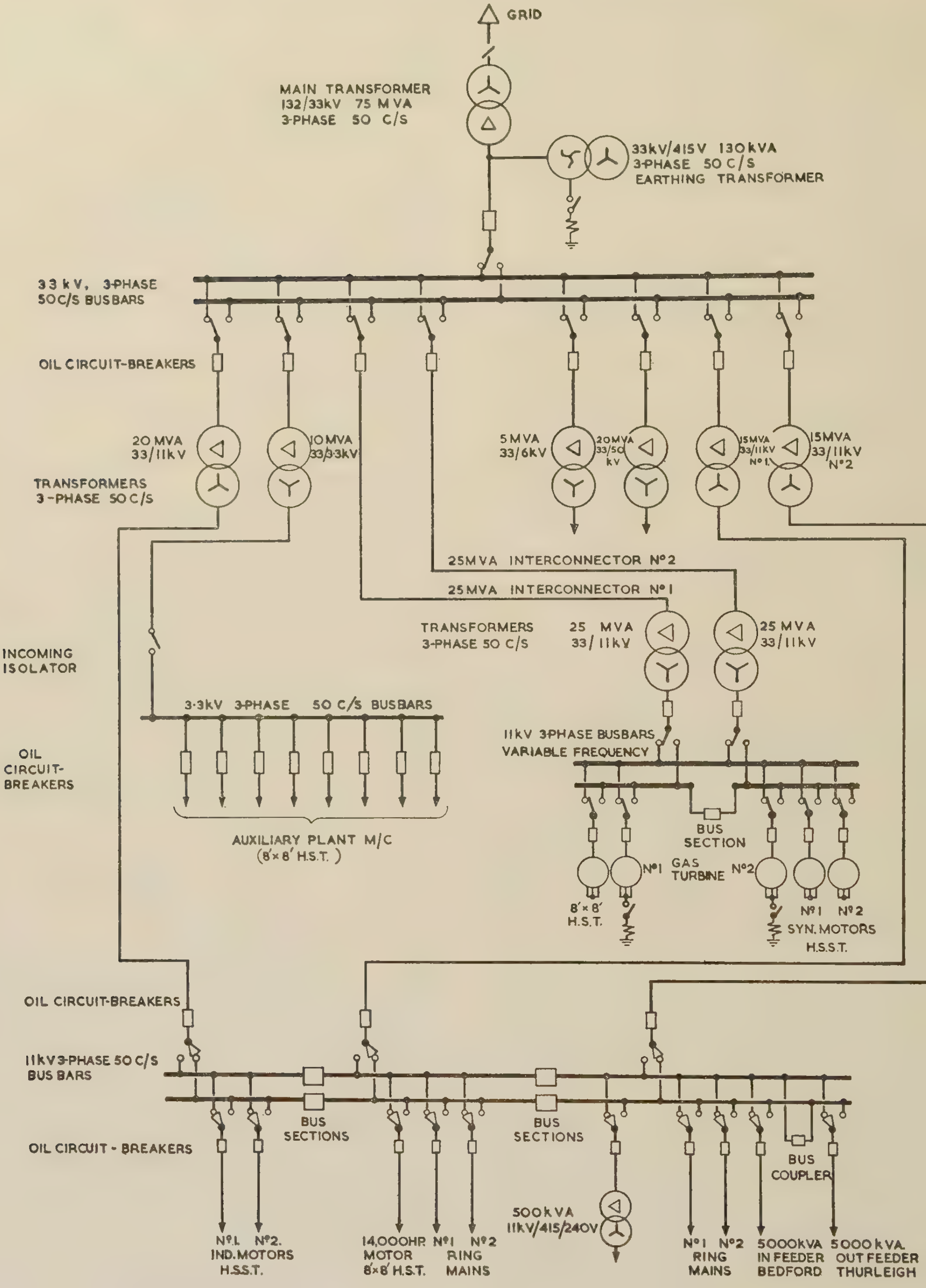

Fig. 3.—Electrical power distribution system at the R.A.E., Bedford.

to keep below the tripping point of the relay. Since voltage is proportional to frequency, and current is thus made inversely proportional to frequency, the maximum power demand on the turbines is kept within the constant horse-power rating over this region.

(4) C.E.A. GRID SUPPLY

The C.E.A. Grid supply to the Establishment is received via 132 kV transmission line, transformed locally to 33 kV and connected to the 33 kV 750 MVA double-busbar switchboard in the site main substation (see Fig. 3). From this switchboard feeders go direct to centres of large load densities where the voltage is again transformed locally to the required load voltage, e.g. 50, 11 or 3·3 kV.

The 33 kV switchboard is connected to an 11 kV 250 MVA double-busbar switchboard, also in the site main substation, via two 15 MVA transformers. This 11 kV switchboard is the centre of the site 11 kV distribution system, which is based on two

11 kV 400 amp ring mains. At ring-main substations the supply is stepped down to a 415-volt 3-phase four-wire system for l.v. distribution for tunnel auxiliaries, machine shops, cranes, lighting, small power circuits, etc.

As previously described the 33 kV switchboard in the site main substation is also connected to the switchboard in the variable-frequency substation via two 25 MVA 33/11 kV interconnecting transformers.

(5) THE R.A.E. 8 FT HIGH-SPEED WIND TUNNEL

The R.A.E. 8 ft high-speed wind tunnel is the first to be connected to the Grid-supply/variable-frequency supply system outlined above, and it has been designed and constructed concurrently with the variable-frequency generating station. The tunnel covers both supersonic and subsonic working, and is therefore a good example of a multi-purpose tunnel described in Reference 1.

It is designed primarily for tests at supersonic speeds up to Mach number 2·8, and the 8 ft square working section is large enough to accommodate models up to about 6 ft span, which allows adequate representation of control surfaces and other details. Tests at low speeds can also be made to investigate conditions at take-off and landing.

(5.1) Layout and Design

The tunnel structure which forms the closed circuit for the circulating air is a large pressure vessel and consists of four horizontal legs generally circular in cross-section. The two long legs are 350 ft and the two cross legs are 60 ft between centres. The diameters vary between 20 ft at the inlet to the compressor and 47 ft at the tunnel cooler. The whole structure, which weighs 5 000 tons, was welded on site to Lloyds pressure-vessel standards.[3] The tunnel layout is shown in Figs. 4 and 5.

The compressor which drives the air round the circuit is a ten-stage axial machine, which, for more efficient working at the lower pressure ratios, can be run as a four-stage machine by replacing the six high-pressure stages with a dummy casing.[4] Both the compressor casings and the dummy casing form part of the tunnel structure when in circuit. Control of the speed of the compressor down to 20% of maximum is required, and the compressor has been designed to give reasonably good efficiency over the full speed range.[4]

The air from the compressor reaches a maximum temperature of 165° C, and having turned through corners Nos. 3 and 4, passes through an air/water heat exchanger of the air-through-tube type to the settling chamber.

The air then passes through the working section, which is rectangular in shape and in which the side walls are fixed and the top and bottom walls are flexed to form a convergent/divergent nozzle.[5,6] The air accelerates through the nozzle, and if sonic speed is reached at the throat and the pressure at the downstream end of the divergent section is less than the pressure at the throat, supersonic speeds are reached in the divergent part of the nozzle.

At the downstream end of the nozzle is mounted the model under test, and this can be rotated to vary its angles of incidence and roll over a limited range. The model is viewed indirectly through a periscope system and by a closed-circuit television system. There is also a Schlieren light system which displays the air-flow pattern round the model. Pressure distribution over

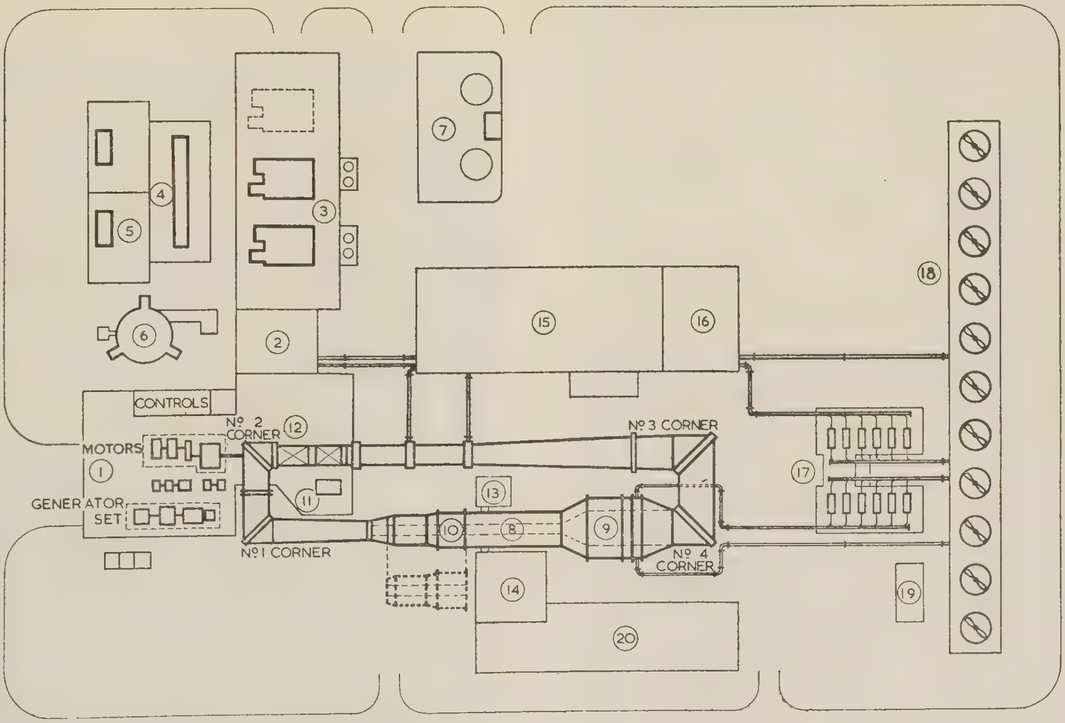

Fig. 4.—Layout of the 8 ft high-speed wind tunnel and generating station, at the R.A.E., Bedford.

1. Main drive plant.
2. Central control room.
3. Variable-frequency generating station.
4. Variable-frequency substation.
5. Transformer park.
6. Load test tank.
7. Fuel-oil storage.
8. Working section.
9. Main tunnel cooler.
10. Supersonic diffuser.
11. Dummy casing.
12. Main-tunnel compressor.
13. Schlieren building.
14. Observation room.
15. Auxiliary plant.
16. Model tunnel room.
17. Pump houses.
18. Cooling tower.
19. Water treatment.
20. Administration building.

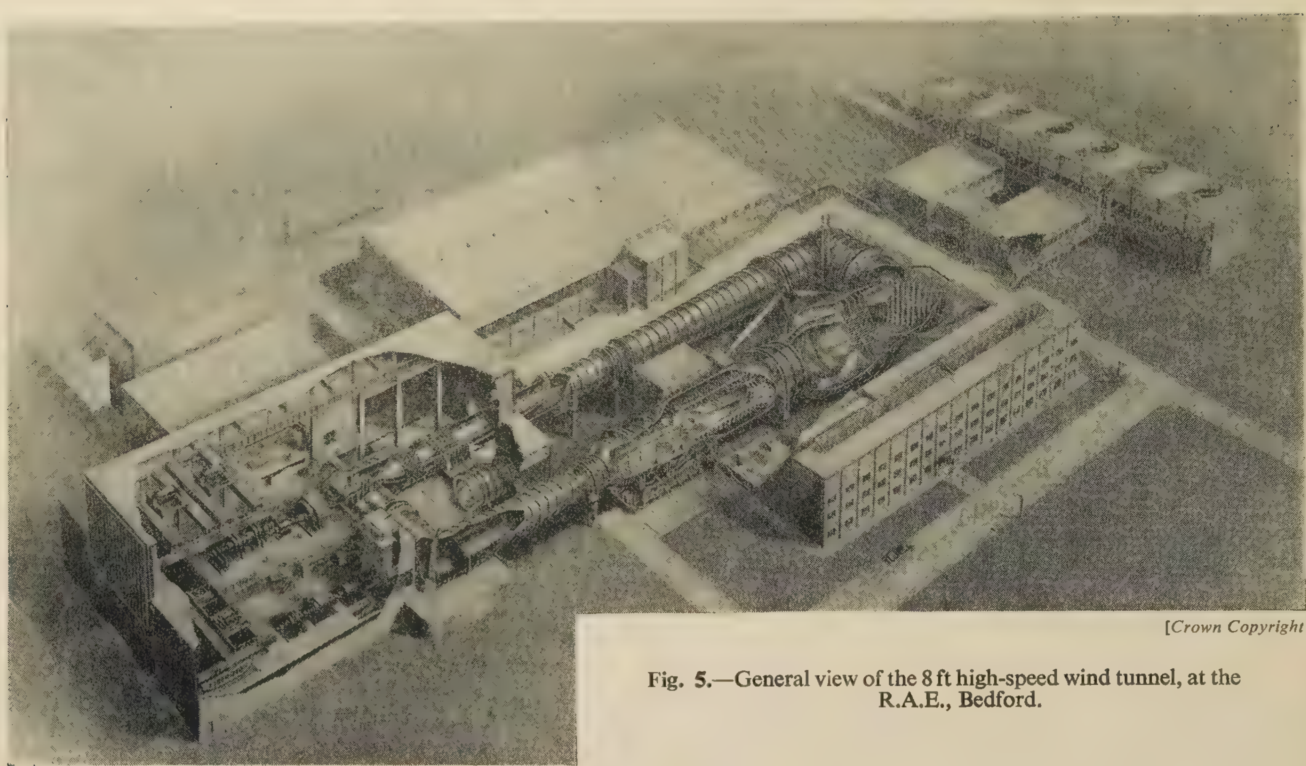

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Fig. 5.—General view of the 8 ft high-speed wind tunnel, at the R.A.E., Bedford.

the surface of the model is measured by orifices connected to manometers. Strain gauges in the model measure the stresses.

After flowing through the working section, the air passes through the supersonic diffuser, where it is slowed down to subsonic velocities, and then passes via a subsonic diffuser round corners Nos. 1 and 2 to the inlet of the compressor.

For the tests to be carried out in this tunnel it is necessary to be able to vary the Reynolds number over a wide range, and for this reason the air pressure can be varied between 0·1 and 4·0 atm absolute. To deal in this way with the tunnel air volume of 435 000 ft^3, auxiliary compressors and evacuators totalling some 10 000 h.p. have been installed. To prevent condensation in the working section, the air in the tunnel is dried to 0·000 5 lb/lb, which is equivalent to a dewpoint of −23° C at 1 atm. Induced-draft cooling towers and circulating pumps with an installed capacity of 2 750 h.p. have been provided to dissipate 500×10^6 B.Th.U. per hour from the water circulated through the main tunnel cooler and the many water coolers associated with the tunnel plant and the generating station.

(5.2) Driving Motors

The main compressor is driven by a direct-coupled 68 000 h.p. 8-pole synchronous motor wound for 11 kV at 50 c/s, and, through a speed-reducing 750/600 r.p.m. double-helical gear-box, by two 6 000 h.p. d.c. motors in tandem, as shown in Fig. 6. The two d.c. motors are supplied by two similar machines acting as generators in a simple series loop under Ward Leonard control. The generators are driven by a 14 000 h.p. 11 kV 50 c/s 10-pole synchronous motor which is reactor-started under automatic sequence control. Fixed excitation from its shaft-driven exciter is designed to give unity power factor at full load. Fig. 7 shows the complete motor-generator set.

(5.3) Operation

In order to reduce the load on the d.c. motors when starting, the compressor is unloaded by evacuating the tunnel to 0·1 atm

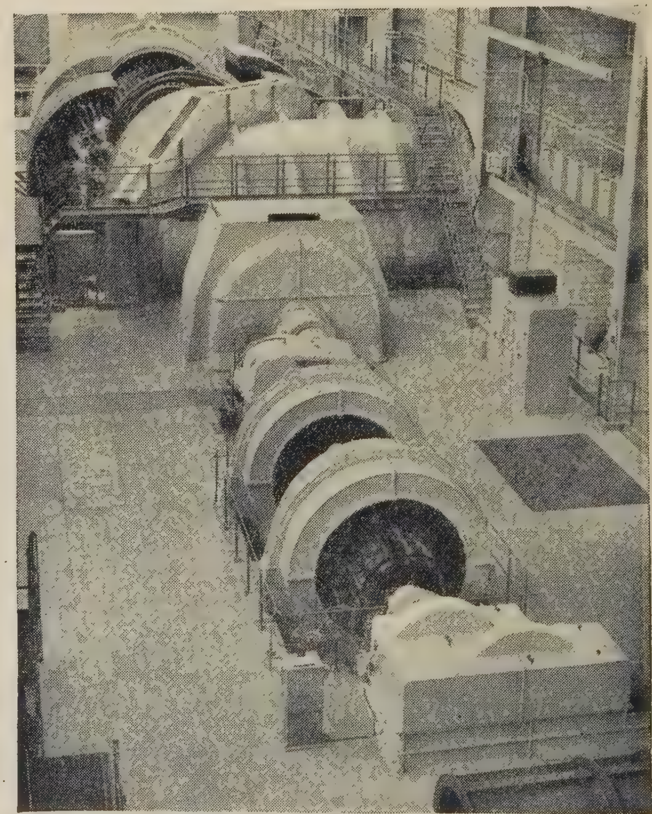

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Fig. 6.—Driving motors for the main compressor of the 8 ft high-speed wind tunnel, at the R.A.E., Bedford.

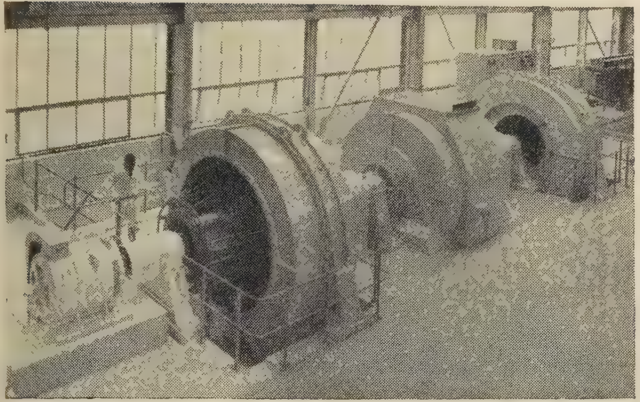

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Fig. 7.—14 000 h.p. motor-generator set for Ward Leonard control of the d.c. motors, at the 8 ft high-speed wind tunnel, R.A.E., Bedford.

absolute, and jacking oil is injected into the compressor and motor bearings. The armature circuit of the d.c. machines is then completed by closing the high-speed d.c. circuit-breakers, and the speed is brought up to the required setting either by manual or by automatic control of d.c. generator excitation. A useful portion of the whole field of operation can be covered by the d.c. motors alone when low compressor speed or low tunnel pressure limits the total drive power to 12 000 h.p. or less.

The second way of running the drive is to connect the 68 000 h.p. synchronous motor to the C.E.A. supply. The starting procedure is exactly the same as described above when running on the d.c. motors alone, but the compressor is driven up to the 50 c/s synchronous speed and the main a.c. motor synchronized with the Grid. Thereafter the speed is constant and the d.c. machines can be set to give a fixed torque. When shutting down, the load is reduced until the d.c. motors alone are driving the compressor, and when the a.c. motor has been off-loaded, its a.c. circuit-breaker is tripped and the drive is then brought to rest by regenerative braking on the d.c. machines. Current limiting of the d.c.-machine armature currents is employed, and this controls the regenerative braking until the compressor speed has fallen to a low value, at which point the d.c. generator fields are 'suicided' and the compressor is finally brought to rest by the use of a hand-operated brake.

The third method of running is with the a.c. motor connected to the variable-frequency supply. Starting procedure is exactly the same as for the previous methods, but in this case the synchronous speed of the a.c. motor is 10 c/s instead of the 50 c/s required when synchronizing with the Grid. After synchronizing, the compressor is run up to the required speed either by hand or automatic control.

When running on the gas turbines under automatic control, load changes are absorbed by the d.c. motors within the limits of their capacity. The changing d.c. current is then arranged to regulate the fuel-oil-flow valve in such a manner as to cause the turbines to take over the change in load and allow the d.c. motors to return to their minimum power datum setting in readiness to compensate for any further changes in load. On fully manual control, d.c. load is adjusted by increasing or decreasing the fuel-valve setting, and under both manual and automatic conditions, where load transfer would overload the d.c. motors, a current-limiting system holds the direct current within the safe maximum rating.

In the event of the d.c. portion of the drive being out of service it is possible to run the drive by switching in the main motor with the variable-frequency alternators at low frequency and starting up as an induction motor. Synchronization would then be effected by switching on the main motor excitation and slightly reducing the alternator frequency, when the motor and turbines would fall into step.

(5.4) Protection

One important requirement on this project is that, in the event of any failure of plant, the speed should be held constant or even increased and not set to zero as in most industrial installations. This is because the transition from supersonic to subsonic flow produces considerable disturbances in the air flow, which may cause damage to the model if the operation is not properly controlled. For this reason, fault detection devices are arranged to shut down the drive only when damage would otherwise be caused to major mechanical or electrical plant, such as main bearings or main motor windings.

As the only means of reducing the speed of the main rotating mass quickly is by regeneration on the Ward Leonard system, it has been arranged that this system shall be kept running until damage to the Ward Leonard equipment itself is imminent.

This has led to very thorough provision of alarm and fault indication throughout the plant, since by early indication of an incipient fault the plant may be 'nursed' until the tunnel conditions are brought to a state where safe shut-down of the drive can occur.

From the point of view of risk to the model, the most critical time for a reduction in speed to occur is when supersonic flow is established in the working section with the tunnel under pressure and the model at high incidence. For use under these conditions an emergency pushbutton is provided for the tunnel operator, which, in addition to operating warning annunciators in all control rooms, initiates a relay system which opens the tunnel blow-off valve and operates the valves of the compressor and evacuator circuits in such a way as to reduce the tunnel pressure as quickly as possible. This rapid lowering of tunnel pressure reduces the air loading on the model and ensures that the flow disturbances during shutting down do not endanger the model. On receipt of this signal the test engineer reduces the incidence of the model and then brings the drive to rest under safe and controlled conditions.

(5.4.1.) Current Limitation

It will be seen that under certain conditions of running, particularly during starting and changing speed, the full output of the electronic amplifier could be applied to the control field and so cause a high overload current in the main d.c. loop. Additionally, various system faults and load-sharing fluctuations between the a.c. and d.c. motors could cause excessive direct current. To counteract any such rise in current, two methods of current limitation are used; first the main current-limit system, which acts directly on the pilot exciter by means of two additional field windings, and secondly the current-limit system associated with the electronic amplifiers, which only functions when the automatic speed control is in use.

In the main current-limit system two field windings are connected in a bridge circuit and fed through two magnetic amplifiers arranged to balance out mains-voltage variations. The control windings of the magnetic amplifiers are connected across the d.c.-generator interpole windings from which a voltage proportional to direct current is derived, and in series with this circuit is a bias system which suppresses this voltage while the direct current varies between the maximum motoring and maximum generating value of 6 000 amp. The control windings are arranged to give a constant 50% output from the magnetic amplifiers during this quiescent period, and as the

connections to the field winding network are arranged to give opposing polarity there is no resultant field produced.

If now excess current occurs in the motoring condition, the bias voltage is overcome and current passes through the control windings of the magnetic amplifiers in such a manner as to cause a proportional unbalance of the bridge-connected field windings. The resulting current flow produces an opposing flux in the pilot exciter, thereby cutting down the generator voltage and reducing the main direct current.

When the Ward Leonard system is regenerating, which can occur at any time during load changes as well as during normal shut-down or emergency tripping action, the voltage across the interpoles is reversed, and when this voltage rises above the bias voltage with excess main direct current, the current flow in the magnetic-amplifier control windings is in the opposite direction. This causes an out-of-balance voltage to drive current round the bridge-connected field-winding loop in such a direction as to increase the flux in the pilot exciter and to raise the main generator voltage.

The net e.m.f. in the main d.c. loop is thus reduced and cuts down the main direct current.

(5.5) Speed Control

An essential requirement of wind-tunnel operation is to maintain constant and steady air-flow conditions in the working section, and these conditions can only be achieved by precise control of temperature, pressure, and compressor speed. In the 8 ft × 8 ft high-speed tunnel compressor, speed is specified to be held within 0·1% of set speed, and while dial calibration to this accuracy is not required since air speed and not drive speed is the test parameter, adjustment of speed in steps of at least 0·1% over the range of 20–100% is necessary.

While very accurate control can be achieved by an operator manipulating a fine rheostat in accordance with the readings on the extremely accurate instruments used in wind-tunnel work, the strain over a long period of working is prohibitive and automatic speed-holding equipment is a necessity.

Unlike most industrial drive control, where output is dependent

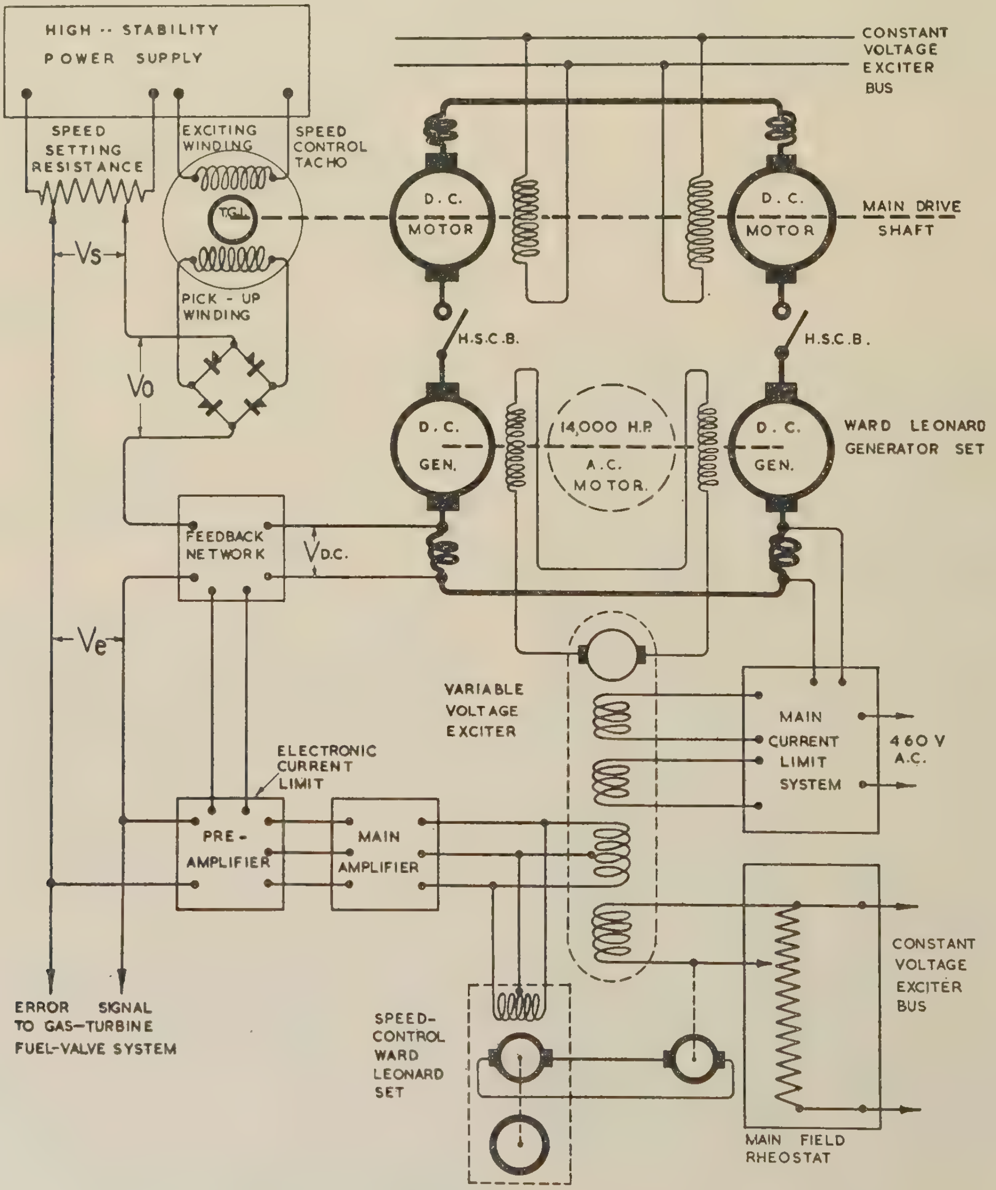

Fig. 8.—Diagram of the d.c. speed-control system.

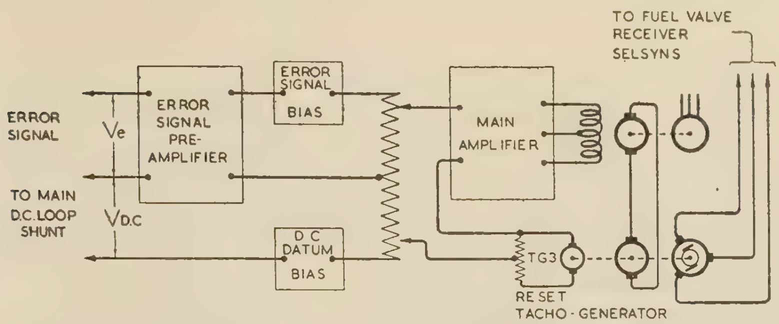

Fig. 9.—Diagram of speed-control link with gas-turbine control.

on high acceleration and quick reversal, the control system of a wind tunnel has to deal with comparatively slow changes, and the aim of all concerned in tunnel operation is directed towards maintaining steady conditions.

For the 8 ft tunnel-drive control, therefore, it was decided to rely on an inherently stable drive system with automatic-speed-control equipment of conventional design to cater for slow drift in external parameters such as mains voltage and ambient temperature.[7]

Advantage has been taken of the 12 000 h.p. Ward Leonard motors to make this the controlling section of the drive by arranging for both the manual and automatic speed-control equipment to operate through the pilot exciter of the Ward Leonard generator as shown in Fig. 8.

The automatic control system follows the accepted method of voltage comparison between the output of a tacho-generator coupled to the drive shaft and the voltage setting of a potentiometer fed from a constant voltage source. The resulting error signal, suitably modified by stabilizing signals, is then amplified and applied to the generator fields in such a manner as to restore the speed setting and cancel the error signal by the consequent change in shaft speed.

In view of the high constancy of speed required, exceptional precautions have been taken with each unit of apparatus in the detecting and amplifying network.[7] Considerable research has been undertaken to obtain a constant rectified output from the gear-driven inductor-type tacho-generator in order to produce a voltage proportional to shaft speed with the least possible divergence.

The power output of the speed-control amplifier feeds a separate field winding on the main generator exciter to give about 10% excitation with full amplifier output in either direction. Additionally the amplifier output is connected to a small Ward Leonard generator, the output of which operates the exciter rheostat motor in such a manner as to reduce the amplifier output by applying the corrective field necessary to restore set speed from the main excitation circuit rather than from the amplifier. Thus, under steady-state conditions the d.c. generator output, and hence the compressor speed, is controlled by the main rheostat setting, while under transient conditions the full amplifier output is available for corrective action in either direction.

The system outlined above can function as an independent unit when the drive is being run on the d.c. motors alone, but when the variable-frequency power is required in addition, a further unit of automatic control of the gas turbines is brought into use, as shown in Fig. 9. This unit accepts signals of speed error and direct current from the d.c. system, and automatically adjusts the gas-turbine fuel-valve setting in such a manner as to cancel the speed error and reduce the direct current to a preset figure, either by increasing the gas-turbine power for an increased shaft-power demand or by reducing the gas-turbine power when the d.c. motors are regenerating owing to reduced tunnel power.

(6) ACKNOWLEDGMENTS

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[The discussion on the above paper will be found on page 228.]

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SPEED CONTROL OF LARGE WIND TUNNELS

With Particular Reference to the R.A.E. 8 ft × 8 ft High-speed Wind Tunnel

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SUMMARY

The paper describes an electronic speed control which has been installed for the composite drive of the 8 ft high-speed wind tunnel at the Royal Aircraft Establishment, Bedford. A description of the requirements of a wind-tunnel speed control is given, and several methods of control are discussed. The selected control system, which is discussed in detail, although of the conventional closed-loop technique, has special problems when gas-turbine prime movers power part of the composite drive. A full description of these special features is given, together with a mathematical analysis of the closed-loop system.

(1) INTRODUCTION

Wind-tunnel work consists in driving a column of air over the surfaces of a model so that its behaviour can be analysed. A fundamental aerodynamic requirement is that the velocity of the air flow in the working section should be constant during the period that balance readings of the forces on the model are being taken. Consequently accurate speed control of the compressor is required over the normal working range.

In subsonic work the air velocity in the working section is directly related to the compressor speed, and thus the control of the aerodynamic parameter $\frac{1}{2}\rho v^2$ is primarily a function of compressor-speed accuracy.

In supersonic work the air velocity in the working section is dependent, for a given compressor speed, on the throat characteristics of the convergent/divergent flexible wall upstream of the model. Speed setting and holding accuracy is mainly concerned with positioning and restraining movement of the shock waves which form at air speeds above Mach 1.

The transition from supersonic to subsonic flow occurs at a strong 'tunnel shock', and the position of this must be kept well downstream of the model. The shock moves downstream as the pressure ratio, i.e. compressor speed, is increased, and upstream as it is reduced. Hence an unexpected speed reduction is dangerous. An unnecessarily high speed, although quite safe, is extravagant of power, and good speed control is necessary to obtain a satisfactory compromise.

(2) REQUIREMENTS OF A SPEED CONTROL

A typical model study, for the type of tunnel considered, would be to run a series of tests over a range of Mach numbers, each test comprising a series of balance readings at progressive changes in model incidence. During each change of incidence, fine adjustment of speed, flexible-wall position and air pressure would be made to restore the required Mach number and reposition the shock wave (with supersonic working).

A further factor requiring consideration in wind-tunnel work is the Reynolds number. This is associated with the scale effect and expresses the ratio of the pressure forces to the viscous forces for any given flight condition

$$\frac{\text{Air density} \times \text{Relative velocity} \times \text{Length of object}}{\text{Viscosity of the air}}$$

The range of Reynolds number for an actual aircraft over its normal speed range may be $5\text{–}25 \times 10^6$, and although it is not possible to obtain these flight values in a wind tunnel, the test Reynolds number is made as high as possible.

The control system should be capable of holding the compressor speed within the specified accuracy limits during the period of test at a given Mach number, which may be of 30 min duration. The model would then be returned to zero incidence and a new Mach number obtained by setting the air pressure, compressor speed and flexible wall to new values. During this period prior to a new test run, transient deviations in speed-holding accuracy can be tolerated, but the settling time should be short.

It is an important characteristic of most tunnels that sudden changes of aerodynamic loading are unlikely under normal test conditions.

Extreme accuracy of compressor-speed indication is not normally necessary. The aerodynamic parameters are the criteria, and these can be more accurately set by reference to the sensitive instruments in the air circuit. More importance is attached to the ability to adjust and hold the compressor speed within fine limits.

Owing to the dangerous conditions which would result if sudden and large changes in compressor speed occurred during supersonic working, failure of the automatic speed control during steady-state or near steady-state conditions must not greatly affect the drive speed.

Manual speed control during tests would impose a strain on the operators if high accuracy is to be maintained, and therefore its inclusion is for standby control only.

(3) THE 8 FT HIGH-SPEED TUNNEL DRIVE

(3.1) General

The 8 ft high-speed tunnel drive shown in Fig. 1 provides a maximum output of 80 000 h.p. to the 10-stage axial compressor and has a normal working range of 150–750 r.p.m. The machine arrangement comprises an 11 kV 50 c/s 68 000 h.p. salient-pole synchronous motor, directly coupled to the compressor via a Cardan shaft, and two 750-volt 600 r.p.m. 6 000 h.p. d.c. motors coupled to the synchronous motor via a double-helical gearbox.

A local generating station provides an alternative variable-frequency supply for the main motor. Two 20 MW alternators are installed, each powered by a gas-turbine prime mover.

The control of the drive speed, tunnel conditions and recording of aerodynamic data takes place from the observation room.

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Messrs. Fox and Gunnell are with the English Electric Co., Ltd.

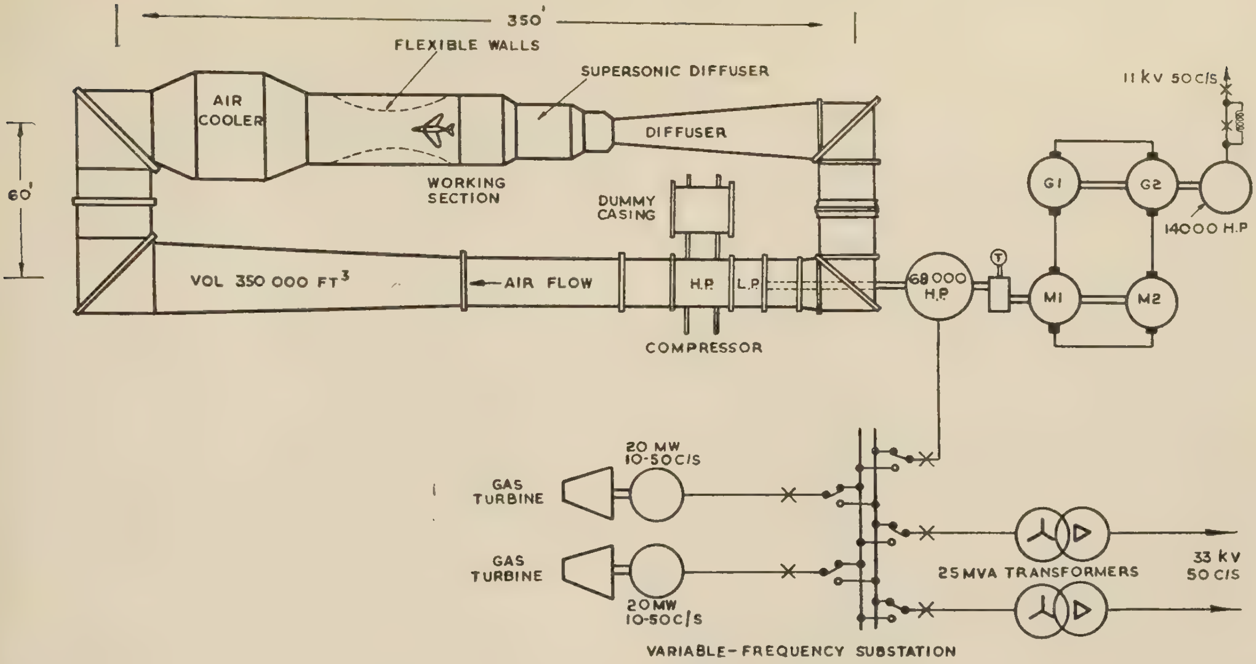

Fig. 1.—Layout of the 8ft high-speed wind tunnel.

(3.2) Compressor

The aerodynamic loading on the compressor can be varied by changing the tunnel air pressure over the range 0·1–4·0 atmospheres. In addition, the compressor itself can be split into two sections—a high-pressure section of six stages and a low-pressure section of four stages. For low-power work the high-pressure stage, complete with its associated tunnel casing section, is retracted from the tunnel by electro-hydraulic auxiliaries and replaced by a dummy casing to provide closed air-circuit continuity.

(3.3) D.C. Machines

The d.c. machines provide a constant torque characteristic over the full speed range, and are supplied by two similar machines, operating as generators, in a series loop under Ward Leonard control and driven by an 11 kV 50 c/s 14 000 h.p. salient 10-pole synchronous motor.

(3.4) 68 000 H.P. Motor

The rating of the 68 000 h.p. synchronous motor was based on the wind-tunnel design requirement of 49 000 h.p. at 540 r.p.m., corresponding to 37·2 MW at 36 c/s. Therefore, a maximum output of 68 000 h.p., corresponding to 52 MW 11 kv at 50 c/s, is available when operated from the national Grid, with voltage and horse-power proportional to speed over the range 10–50 c/s when supplied from the variable-frequency alternators.

(3.5) Gas Turbines and Variable-Frequency Alternators

The gas-turbine power/frequency characteristic shown in Fig. 2 is approximately parabolic in shape, flattening out in the upper part to give an output of 19–20 MW over the frequency range 36–50 c/s. Each gas turbine has four independent shaft systems, comprising two stages of compression with interstage cooling, followed by two stages of expansion. The speed and load are governed by an electrically-driven fuel valve with an overriding limit provided by a hydraulically-controlled governor.

The 2-pole 3-phase variable-frequency alternators are rated at [illegible] MW, 7·92 kV, at 36 c/s with a voltage proportional to frequency over the range 10–50 c/s. The full capacity cannot be utilized above 36 c/s owing to the flat characteristic of the gas turbine.

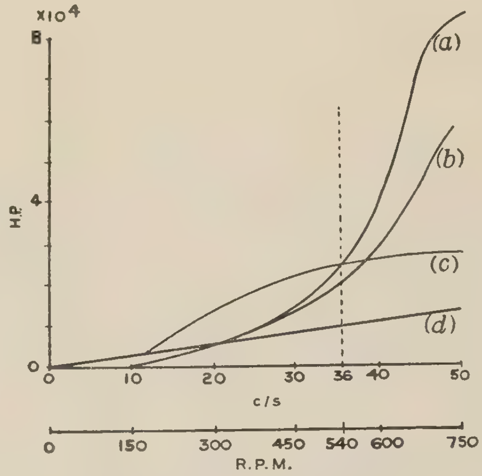

Fig. 2.—Machine characteristics.

(a) Compressor (high pressure).
(b) Compressor (low pressure).
(c) Gas turbine.
(d) D.C. motors.

(3.6) Methods of Operation

The compressor changes, together with the tunnel air-pressure range described in Section 2, give a wide scope of operation at various speeds and loads:

(a) D.C. machines only	Variable-speed low power. (12 000 h.p.)
(b) D.C. machines + A.C. motor supplied from Grid system	Fixed speed. (Variable power with maximum of 80 000 h.p.)
(c) D.C. machines + A.C. motor supplied from variable-frequency alternators	Variable speed. (Variable power with maximum of 66 000 h.p. at 750 r.p.m.)

(4) DESCRIPTION OF SPEED-CONTROL REQUIREMENTS

The speed control was designed to meet the following requirements:

(i) Completely automatic control for operations (*a*) and (*c*), with automatic control of the drive up to Grid synchronizing speed for operation (*b*).

(ii) Ability to change from automatic to manual speed control, or vice versa, under stable speed conditions without materially disturbing the tunnel air flow.

(iii) Single-knob control for automatic speed setting, continuous over the range 75–750 r.p.m. and incorporating fine adjustment changes of less than 0·1% speed.

(iv) Speed-holding accuracy must be within ±0·1% of the set speed over the normal working range 150–750 r.p.m. and ±1·0% over the range 75–150 r.p.m. A departure from this accuracy to be tolerated during variation of tunnel conditions between tests.

(v) The automatic speed control should permit safe starting from rest, with controlled acceleration of d.c. machines.

(vi) Failure of the automatic speed control system during steady tunnel conditions should not cause major change in speed.

(vii) Both manual and automatic speed control to permit emergency shut-down under controlled conditions by regeneration in the d.c. loop.

(viii) Selection of speed control to give (*a*) automatic control operative on d.c. machines alone, and (*b*) automatic control operative on d.c. machines and gas turbines. The selection of the latter control is to be made after the drive speed reaches 150 r.p.m.

(5) DISCUSSION OF POSSIBLE SPEED-CONTROL SYSTEMS

Prior to the establishment of the detailed speed requirements and the selection of the voltage comparison system, consideration was given to a number of speed-error detection methods, utilizing single-knob control and capable of 0·1% accuracy over a 5 : 1 speed range, particularly to the role of the gas-turbine control. The following Sections give a brief résumé of the most promising methods.

(5.1) Phase Control with Frequency Reference

The method would consist of matching the phase angle of an a.c. tachogenerator coupled to the drive with that of an oscillator reference, so that the frequency of the drive in the steady-state condition was always in synchronism with the oscillator. The frequency setting of the oscillator would have to cover the desired speed range and the output of both oscillator and tachogenerator fed to a phase-sensitive unit. The difference in phase is measured, and the resultant output amplified for control purposes.

This type of control system would require the drive speed to be brought to the set value and synchronized by additional control equipment.

Experience with other tunnels has shown that there is an upper limit to the frequency which can be used, since the drive must pull into synchronism between a positive peak and the succeeding negative peak of the difference-frequency sine wave. The allowable difference frequency depends on load conditions and the ratio of accelerating torque to drive inertia. Reduction of the oscillator nominal frequency to increase the period during which synchronization of the drive can take place, i.e. increase of the allowable percentage difference frequency, might lead to the drive speed swinging outside the speed tolerance while remaining within the synchronous range.

The optimum frequency range of a small wind tunnel having this form of control with a 5 : 1 speed range and a mechanical time-constant of 1·2 sec, was found to be 0·8–4 c/s, with a permissible difference frequency for synchronization of 0·1 c/s.

The 8 ft tunnel has a mechanical time-constant of 13 sec referred to the d.c. machines. Thus, neglecting the effect of increased air mass and machine-capacity limitations, and assuming strict proportionality with the tunnel referred to above, the reference frequency for a 5 : 1 speed range would be 0·074–0·37 c/s. Attendant problems are:

(*a*) The additional time delay due to the generator fields, since, in the tunnel referred to previously, the motors are supplied by mercury-arc rectifiers.

(*b*) The limited torque available for rapid response requirements if synchronism is to be maintained, since, for reasons given later, the gas turbine must be limited to slow response.

(*c*) With larger mechanical and electrical time-constants, the upper frequency limit must be reduced to a value which makes it necessary for the synchronizing accuracy to approach, and perhaps exceed, the speed accuracy.

(5.2) Frequency Control with Frequency Reference

Certain advantages are obtained over phase control by replacing the reference oscillator with a frequency-sensitive bridge having a range equal to the desired speed range, and comparing this with an a.c. tachogenerator or revolution-counting device operated from the drive. The drive need only be brought to within the frequency range of the bridge, and adjustment of speed setting can take place without loss of speed control.

The frequency-sensitive bridge produces a voltage signal, at the tachogenerator frequency, with an amplitude proportional to the frequency difference of the tachogenerator and bridge setting. This signal is capable of a.c. amplification before being fed to a phase-sensitive rectifying stage.

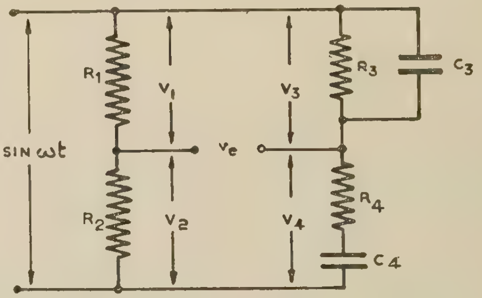

Fig. 3.—Wien bridge.

Using a Wien bridge (Fig. 3), which utilizes variable capacitance and is preferable to other bridges using inductances, it can be shown that, for maximum sensitivity, the relationships between the circuit parameters are as follows:

$$\frac{R_2}{R_1 + R_2} = \frac{5}{8}$$

$$R_3 = \frac{3}{2}R_4$$

$$R_2 = R_4 + X_c \text{ and that } 1 - \omega^2 C_3 C_4 R_3 R_4 = 0$$

Also at balance

$$\frac{R_2}{R_1 + R_2} = \frac{R_3 + R_4}{2R_3 + R_4}$$

From the equation for balance, it follows that, if C_3 and C_4 are mechanically coupled and have a linear characteristic of capacitance/vane movement, the bridge will have a set frequency inversely proportional to vane position.

This inverse law is unacceptable, since proportional change of drive speed against dial setting is required. To obtain proportional change using a simple mechanical condenser-plate drive necessitates special shaping of condenser plates.

A study of available condensers indicated that linear movement with drive speed would reduce the available variable capacitance to one-tenth of that given by a semicircular condenser plate. This would impose a speed-range limitation of 5 : 1, since single-knob control was required without recourse to range switches. This capacitance limitation is capable of marginal

improvement by the use of a fluid dielectric, and consideration of temperature coefficient showed the best choice to be a good transformer oil. The latter provided a capacitance increase of $2\frac{1}{2}$ times that for an air dielectric, with a net temperature coefficient of -360×10^{-6} per deg C, which could be compensated in the resistance arm by using a combination of manganin and nickel.

A further difficulty of the system is the design of a suitable phase-sensitive rectifier. This is required in order to detect the change in phase of approximately 180° in the bridge output as the tachogenerator frequency varies above and below the bridge balance frequency. A reference voltage is required of greater magnitude which is either in phase with or in opposition to the bridge output signal. Obtaining such a reference voltage would be extremely difficult.

Consequently, it was considered that, while the Wien bridge method in its then state of development would give greater accuracy than voltage comparison for a limited speed range of 2 : 1 or 3 : 1, its extension to cover the specified range of 10 : 1, utilizing single-knob control, would require comparably greater development than that entailed with a voltage comparison scheme.

(5.3) Voltage Comparison

Voltage comparison control, with a highly stabilized voltage reference and tachogenerator field current, in conjunction with refinement in tachogenerator design, offers, in the authors' opinion, the simplest method of control. A ready means of adjusting the gas-turbine output is thus available, since the error voltage can be used to provide control to the fuel valve, and it is also possible to provide a large range of speed setting which is substantially linear.

(5.3.1) Role of Gas Turbines.

Consideration of the relative power levels contributed to the drive by the a.c. and d.c. machines, would favour the larger source for the duty of master controller. After the d.c. machines had brought the drive to the gas-turbine synchronizing speed (150 r.p.m.), they would be set to contribute a given torque at the test-speed setting, and any departure from the set condition would be corrected by the automatic speed control operating on the gas turbines. This arrangement was rejected for the following reasons:

(*a*) The difficulty of obtaining a turbine governor capable of the speed accuracy required.
(*b*) The problem of load sharing between turbines.
(*c*) If the gas turbine served as the master control the advantage of the inherent low regulation of the Ward Leonard system would be lost.
(*d*) The maximum acceleration is limited by the turbine design to correspond to a load change of 5 000 h.p. per minute.
(*e*) Arrangements would be required whereby the automatic control could be applied to the d.c. machines only, for operation under low-power working.
(*f*) The problem of the gas-turbine response to speed error and the difficulty of obtaining a reliable transfer function for inclusion in a closed-loop speed-control analysis.

Each gas turbine comprises four rotating units connected solely by the gas that flows between them. Thus the speed and temperature will vary as the fuel is varied, and some time delay is inevitable before a correct balance is restored.

Fig. 4 shows a typical response for a step input of fuel, and it is seen that an irregular response on a long time scale is obtained. Consequently the response of the fuel-valve servo mechanism would have to be relatively slow if undesirable oscillations are to be avoided.

With the automatic speed control applied to the Ward Leonard system, the rate of drive acceleration is considerably greater than that obtained from the gas turbines, since the response is

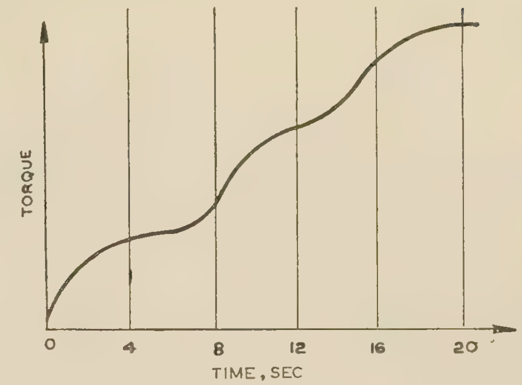

Fig. 4.—Typical response of gas turbine to a step of fuel.

now limited only by consideration of maximum current in the main Ward Leonard loop and the field time-constants of the generator excitation system.

(6) DESCRIPTION OF SELECTED CONTROL

Both automatic and manual speed control is provided. Automatic control is arranged to take place in the d.c. machines alone or in conjunction with the gas turbines. In the latter operation, the gas turbine provides a slow follow-up control, normally unloading the d.c. machines so that their full output is available for rapid correction of speed error.

(6.1) Speed Reference and Tachogenerator Field Supply

In a closed-loop system, random variations in the power supply to the reference or in the tachogenerator could cause a corresponding variation in the control signal, while those variations within the loop, although undesirable, are subject to the loop gain which reduces their effect. Consequently, in order to minimize the effect of random variations outside the loop due to changes in mains supply voltage and temperature effects, special consideration has been given to the design of the reference and tachogenerator.

The supply to the tachogenerator field and reference potentiometer P_1 shown in Fig. 5 is obtained from a stabilized power

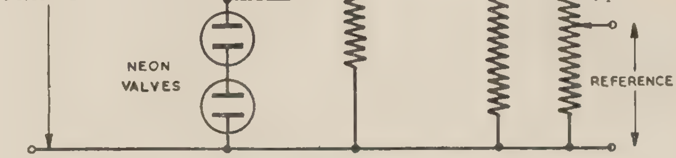

Fig. 5.—Stabilized reference and tachogenerator field supply.

supply using a high-stability reference neon tube. The voltage across the reference resistor is kept constant, and, as this is a low-temperature-coefficient component, the field current is also maintained constant. Thus, should a small change in voltage occur across the reference resistor, a corresponding change of current would occur in the tachogenerator field. With a substantially

linear tachogenerator characteristic, changes in reference voltage are largely cancelled by corresponding changes in the tachogenerator output voltage.

(6.2) Speed Control on D.C. Machines

A high-frequency tachogenerator mechanically coupled to the motor-armature shaft provides a voltage output proportional to the compressor speed. This reset voltage is fed to the rectifier unit and the output is smoothed. The reference voltage is set, by means of the 40-turn helical potentiometer P_1, to be equal to the tachogenerator output at the desired compressor speed.

The reference and reset voltages are compared so that the resultant error voltage is proportional to the deviation from set speed. A stabilizing feedback signal from the tachogenerator is added to the error voltage. This modified error voltage is amplified to provide a push-pull signal and subjected to a current-limit voltage, derived from the series windings of generator 2, to prevent excessive load in the main Ward Leonard loop. The backed off against a preset voltage to give an adjustable current limit.

The design requirement, with regard to failure of the automatic speed control, is satisfied by the provision of the follow-on rheostat. This operates in a direction to provide all the necessary generator excitation, thus reducing the speed error, and consequently the output from the electronic-amplifier-supplied fields.

(6.3) Gas-Turbine Fuel-Valve Control

As explained in Section 5.3.1, the nature of the dynamic characteristics of the gas turbine limits its role to a slow follow-on control. This is achieved by controlling the fuel-valve operation and rate of movement, with a fuel-valve pattern signal which accommodates the turbine characteristic shown in Fig. 4.

Each gas-turbine fuel valve is driven by a Selsyn receiver which remains in synchronism with the Selsyn transmitter coupled to the d.c. motor of the fuel-valve m.g. set. The fuel-valve profile is so graded that, although the fuel-flow/horse-power output

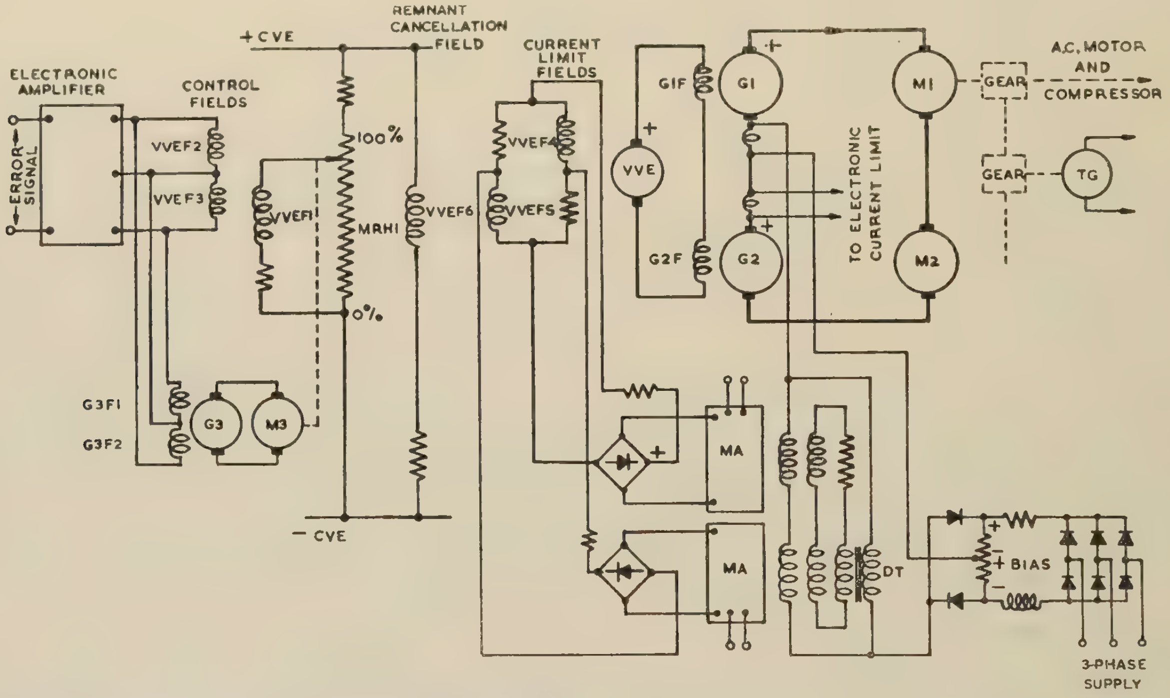

Fig. 6.—D.C. motor speed control.

$VVEF_{1,2,3,4}$ = Variable-voltage exciter field windings.
CVE = Constant-voltage exciter.
MRH1 = Main rheostat ('follow on' rheostat).
MA = Magnetic amplifier.
M1 and M2 = D.C. motors.
G1 and G2 = Ward Leonard generators.
DT = Damping transformer.
TG = Tachogenerator.
G3 = 'Follow-on' generator.
G3F1 and G3F2 = 'Follow-on' generator field windings.
M3 = Pilot motor for MRH1.

combined signal is then further amplified and energizes, as shown in Fig. 6, the following:

(*a*) The push-pull fields of variable-voltage exciter (VVEF2 and 3) in a direction to assist or oppose the main exciting field up to 10% of the total exciter field strength.

(*b*) The push-pull fields of the speed-control m.g. set generator G3 which, via the follow-on-rheostat motor and rheostat, changes the excitation of the variable-voltage-exciter main field (VVEF1).

The variable-voltage exciter provides the excitation level to the generators G1 and G2.

An additional current limit is provided for manual speed control, and back-up of the electronic current limit, by two magnetic amplifiers connected in push-pull and feeding a separate variable-voltage exciter field (VVEF4 and 5). The magnetic-amplifier signal is derived from the series winding of G1 and characteristic is non-linear, the maximum permissible load change of 5000 h.p./min is obtained at maximum fuel-valve motor speed, regardless of the initial position of the valve.

(6.3.1) Fuel-Valve Pattern Signal.

The fuel-valve pattern signal is designed to give an adjustable range of fast follow-up control for gross speed errors, followed by a reduced fuel-valve speed as the regulation range of the d.c. machines is approached.

A simplified diagram of the fuel-valve closed-loop system is show in Fig. 7.

The error voltage derived from the tachogenerator and reference voltage, as previously described, is also utilized for control of the fuel valve. The error signal is amplified and applied to an adjustable bias unit, which cuts off the error signal

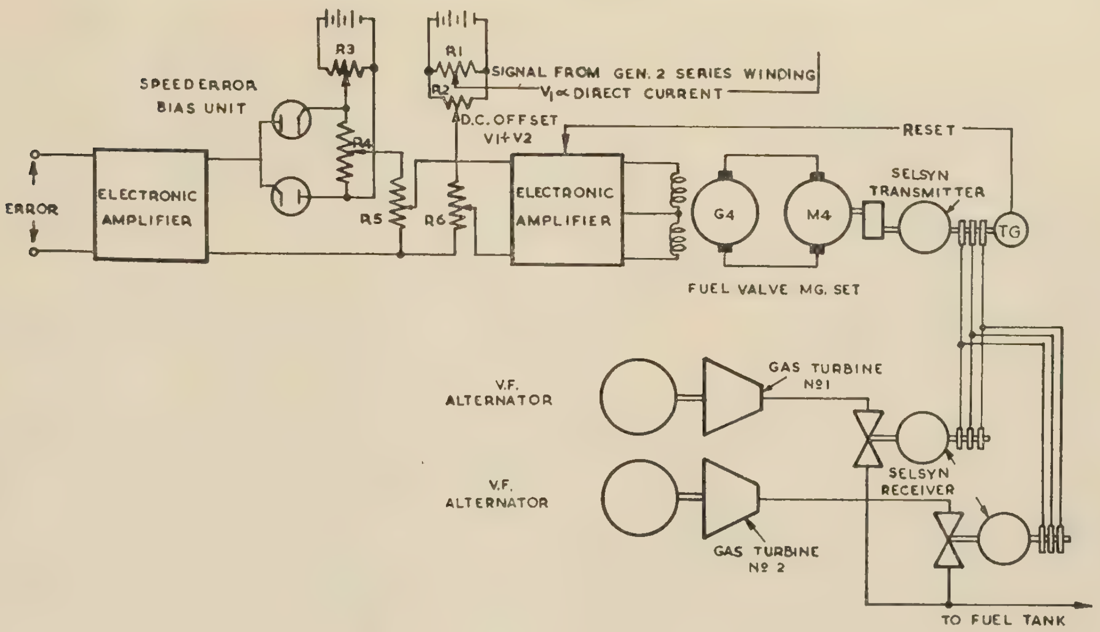

Fig. 7.—Schematic of fuel-valve control.

until the bias setting is exceeded. Reference to the curve of fuel-valve-motor-speed/speed-error in Fig. 8 shows that increase of bias setting increases the cut-off zone and that, by bias unbalancing, an unequal response can be obtained for positive or negative speed errors. Variation of R_5 alters the gain, i.e. the slope of the signal in Fig. 8. In practice, the bias will be balanced and the setting adjusted so that current limit in the Ward Leonard loop is operative in the cut-off zone. This current-limit signal (Fig. 9) is derived from the series winding of G2, and gives a voltage proportional to current which is added to the biased error signal. Adjustment of gain of the main Ward Leonard loop-current signal is provided by R_6, and its effect is shown by the dotted lines in Fig. 9.

The combined voltage shown in Fig. 10 comprises the fuel-valve pattern signal. This signal is split to provide a push-pull signal to the electronic amplifier, and the amplified output is applied to the push-pull fields of the fuel-valve m.g. set generator G4. G4 drives the fuel valve via the motor M4 and the synchronous link.

(6.3.2) Fuel-Valve Motor Tachogenerator.

Open-loop speed control of the fuel valve would be subject to load regulation, and would be inaccurate at low fuel-valve pattern signals owing to variable friction in the fuel-valve drive. Thus, in order to provide proportional control of motor speed to error signal over the full range, and consequently to control to zero error due to the integral control in the main speed control loop, a separate tachogenerator is coupled to the fuel-valve motor M4 to provide a reset voltage proportional to the fuel pattern signal strength.

The servo-mechanism block schematic (Fig. 11) shows that the fuel-valve control provides an output proportional to error, plus the integral of error which results from the fuel-valve position.

(6.4) Current Offset

The normal action of the gas-turbine control is to correct for gross speed error and current in the main Ward Leonard loop. However, to make available increased torque to the drive under steady-state tunnel conditions, a direct-current offset control is provided. This control sets a false 'zero current' datum, and

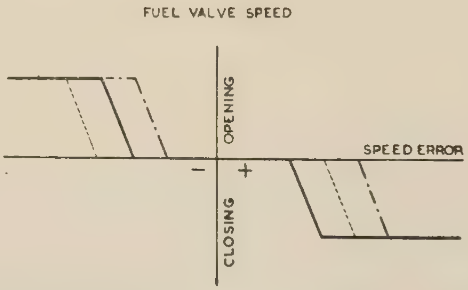

Fig. 8.—Typical characteristic of main d.c. motor speed error and fuel-valve speed.

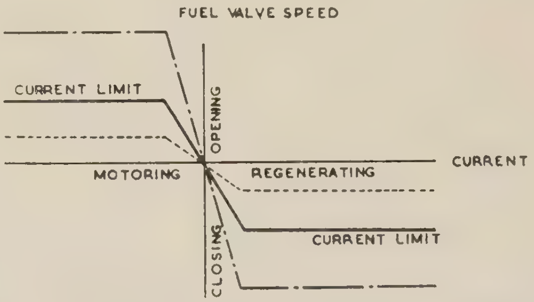

Fig. 9.—Typical characteristic of main d.c. motor current and fuel-valve speed.

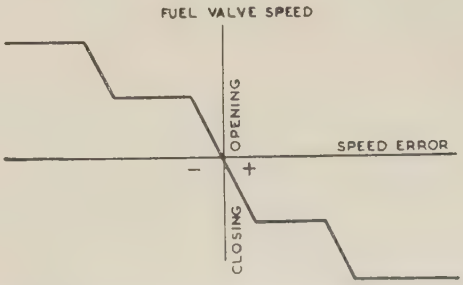

Fig. 10.—Typical characteristic of current and speed error combined.

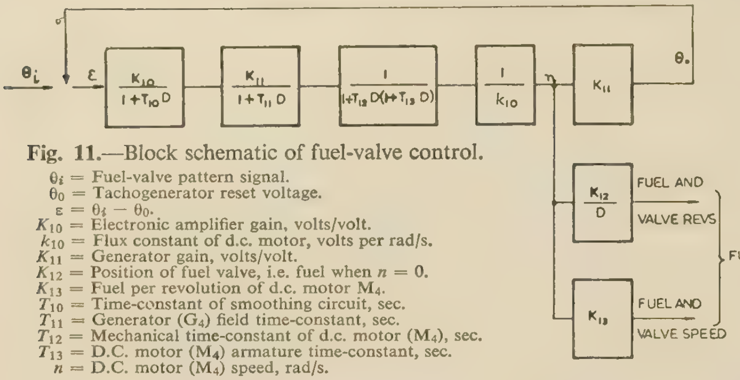

Fig. 11.—Block schematic of fuel-valve control.

θ_i = Fuel-valve pattern signal.
θ_0 = Tachogenerator reset voltage.
$\varepsilon = \theta_i - \theta_0$.
K_{10} = Electronic amplifier gain, volts/volt.
k_{10} = Flux constant of d.c. motor, volts per rad/s.
K_{11} = Generator gain, volts/volt.
K_{12} = Position of fuel valve, i.e. fuel when $n = 0$.
K_{13} = Fuel per revolution of d.c. motor M_4.
T_{10} = Time-constant of smoothing circuit, sec.
T_{11} = Generator (G_4) field time-constant, sec.
T_{12} = Mechanical time-constant of d.c. motor (M_4), sec.
T_{13} = D.C. motor (M_4) armature time-constant, sec.
n = D.C. motor (M_4) speed, rad/s.

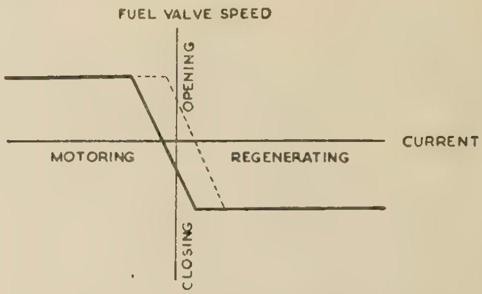

Fig. 12.—Typical characteristic with offset control.

thus provides an adjustable minimum current level in the main Ward Leonard loop to which the gas turbines control at zero speed error.

The offset voltage V_2 (Fig. 7), is developed across the adjustable resistances R_1, R_2 and can be made positive or negative with respect to the signal voltage V_1, proportional to direct current. Variation of the offset voltage moves the direct-current component of the fuel-valve pattern signal along the abscissa (Fig. 12), but does not vary the preset current limit, since, if V_2 increases, V_1 decreases and thus the algebraic sum is constant.

It will also be seen that use of the offset control limits the capacity of the d.c. machines to correct for divergence of drive speed from the set condition.

(6.5) Action of Control

(6.5.1) Operation at Low Power.

The case is considered when the d.c. machines are operating alone and the tunnel conditions are varied slightly causing a small positive speed error, i.e. drive speed below set speed.

The resulting error voltage, which is equal to the difference between the tachogenerator and reference voltages, is amplified and energizes the push-pull fields (VVEF2 and 3) of the variable-voltage exciter in a direction to increase the excitation rapidly. The generator voltage rises and an increased current flows in the main Ward Leonard loop to accelerate the drive. At the same time, the amplified error signal is applied to the push-pull fields of the speed-control m.g. set generator G3, causing the pilot motor M3 to rotate and drive the follow-on rheostat at a speed proportional to the amplitude of the error signal, and in a direction to increase the excitation of the variable-voltage-exciter main field (VVEF1).

As the drive accelerates the error voltage is reduced, thus decreasing the excitation to the variable-voltage-exciter push-pull fields and the travel rate of the follow-on rheostat until set speed is reached. At this position the electronic output is negligible and the follow-on rheostat is stationary with the steady-state excitation provided by the variable-voltage-exciter main field.

For large changes in drive speed, where the current in the main Ward Leonard loop would otherwise greatly exceed full load, the d.c. machines accelerate under current limit control.

For negative speed changes, i.e. drive speed above set speed, the sequence will be as described above except that the variable-voltage-exciter push-pull fields will act to reduce the total excitation, followed by a corresponding movement of the follow-on rheostat.

It will be appreciated that the speed error at which current-limit action occurs will depend on the steady-state load and will generally be 0·1% set speed.

(6.5.2) Operation at High Power.

The case of d.c. machines and gas turbines operating under automatic control with zero-current offset and the fuel-valve speed error bias set at 10% is considered. Consequently, in the steady-state condition, all the necessary drive torque is provided by the a.c. motor supplied from the gas turbine driven alternators.

For speed errors below 10% of top speed the d.c. machines will correct as described above (current-limit control occurring at 0·1% for errors in either direction). As the current increases from zero in the Ward Leonard loop, the voltage signal V_1, proportional to this current (see Fig. 7), is amplified and energizes the push-pull fields of the fuel-valve m.g. set generator G4, causing the d.c. motor M4 to rotate in a direction to increase the fuel-valve setting. Reference to Fig. 9 shows that the fuel-valve opening speed is constant during the period that the d.c. machines are in current limit.

The gas-turbine power output is increased and the speed error is further reduced, if not already corrected by the d.c. machines. At zero speed error the d.c. machines will be contributing a share of the drive power and the action of the gas turbines will tend to raise the drive speed slightly above the speed setting. The resulting error will cause the automatic speed control to reduce the excitation of the d.c. machines. This reduction in the main Ward Leonard loop current reduces the signal to the fuel-valve control, consequently slowing down the opening of the fuel valve until zero direct current is reached, when no further movement of the fuel valve takes place.

For speed errors above 10% of top speed the d.c. machines correct under current-limit control, as previously described. But since the speed-error signal now exceeds the bias setting, the fuel-valve pattern signal includes a component proportional to the speed error, and the resulting amplified output to G4 is increased, causing rapid operation of the fuel valves to assist the d.c. machines in the correction of large speed error.

The maximum rate of fuel-valve movement is limited by the characteristics of the electronic amplifier (see Fig. 8), which saturates at an input equivalent to a gas-turbine rate of load increase of 5 000 h.p./min.

As the speed error is reduced by the combined action of gas turbines and d.c. machines the rate of fuel-valve movement is also reduced, until, at a speed error of 10%, the bias setting cuts off the speed-error component to the fuel-valve pattern signal and operation takes place under control of the signal proportional to the main Ward Leonard loop current, as previously described.

(7) TACHOGENERATOR

One type of tachogenerator, specially developed for high-accuracy speed controls, is a high-frequency inductor-type machine. The rotor is built of laminations, each punched with 60 teeth, so that the generated frequency in cycles per second is conveniently equal to the tachogenerator speed in revolutions

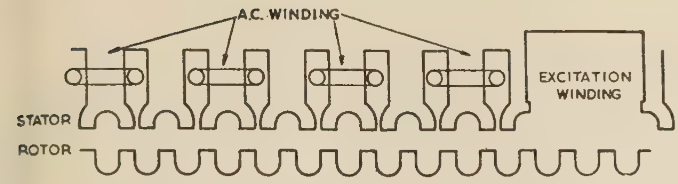

Fig. 13.—H.F. tachogenerator, stator and rotor slots.

per minute. The stator carries both d.c. (excitation) and a.c. (output) coils. Stator and rotor slotting is shown in Fig. 13, from which it can be seen that the air-gap reluctance is constant, being independent of the rotor position relative to the stator. Consequently, less alternating voltage is induced in the d.c. windings.

This alternator is suitable for running over a wide range of speeds; its open-circuit characteristics are more linear than those of d.c. machines, over a working range of excitation values. The hysteresis effect is small. For high-accuracy speed controls using voltage reference it has advantages over d.c. tacho-generators in that there are no brushes or commutators. The output waveform is nearly sinusoidal at all speeds, and with a low modulation inherent in the machine, accurate speed indication is possible.

Difficulty was experienced with some early machines which had a low-frequency modulation superimposed on the high-frequency output voltage. This is especially troublesome in control systems and is overcome by a special type of tacho-generator construction.

The major disadvantage in using an a.c. tachogenerator is that rectification is necessary to compare with the voltage reference of the servo system, and care must be taken to minimize the rectifier's undesirable characteristics if an accurate speed signal is to be obtained.

(8) TESTS ON SPEED-DETECTION CIRCUIT

Works tests were undertaken to serve as a trial of the actual components to be installed on site. The purpose was to determine and improve the accuracy of speed reference and detection over the entire speed range. The causes of inaccuracy were distinguished as follows:

(*a*) Magnetic hysteresis in the tachogenerator.
(*b*) Rectifier temperature variations.
(*c*) Stator air-gap temperature-difference variations in the tachogenerator.
(*d*) Short-term ageing of the rectifier.
(*e*) Overall temperature change of the tachogenerator.

By backing off the rectified tachogenerator voltage against a stabilized voltage a reading was taken, on a sensitive galvanometer, of the change in speed reference.

In tests of this kind it is necessary to achieve extreme accuracy. To control the speed of the motor driving the high-frequency tachogenerator would introduce errors of the same magnitude as those we are trying to determine. The driving motor speed can be made independent of mains-voltage variations, but mains-frequency variations are present.

The technique adopted was to reduce all speed changes to second-order effects. A curve was plotted of galvanometer deflection against speed over a range of mains-frequency variations. Sufficient readings were taken to obtain the slope of the line of best fit. Fig. 14 shows the method used to correct the measured speed for zero galvanometer deflection. Having obtained the slop of the curve (*xy*), a parallel line is drawn through the test point A to intersect the speed axis at the point B. This point is then the corrected speed for the tachogenerator voltage to be exactly equal to the reference voltage.

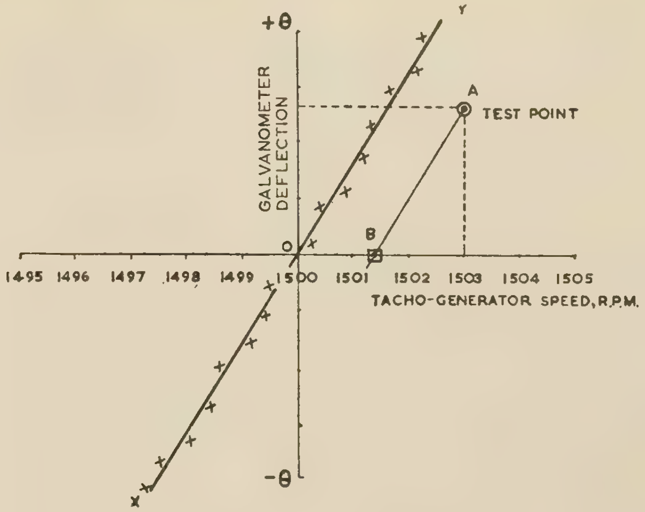

Fig. 14.—Graphical method of speed measurement correction.

Average speed measurements were taken throughout the tests by means of a crystal-gated counter over a period of 10 sec.

At the beginning of the tests, variations in speed reference were $\pm 0 \cdot 03\%$ in half an hour.

By temperature control of the high-frequency tachogenerator, and tacho-rectifiers with smoothing circuit, and a reference potentiometer, the variations were reduced to less than $\pm 0 \cdot 01\%$ in an hour and a maximum drift of $0 \cdot 03\%$ per hour, after 3 hours' running time. Drift is defined as the rate of change of speed reference with time.

By far the biggest change in speed reference occurs shortly after switching on the tachogenerator. In the first 20 min, the variations and drift may be four times as great as the above figures, i.e. reference variation of $0 \cdot 04\%$ and maximum drift of $0 \cdot 12\%$ per hour.

It is clear, therefore, that more accurate results may be obtained if the tachogenerator is allowed time to settle. One of the contributory factors to large initial variations is the difference in temperature between the stator and rotor surfaces. Usually, the field current of the tachogenerator is left switched on, and this temperature difference is caused by a high-frequency iron loss in the rotor after the machine has started to run.

The tachogenerator has a small ratio of gap to diameter of rotor, so that, if the temperature of the rotor changes relative to that of the stator, the length of air-gap is changed by a considerable percentage, with a corresponding change in the generated voltage. By calculation, it is found that an $0 \cdot 1°C$ change in temperature difference gives $0 \cdot 02\%$ change in speed reference. If the temperature were uniform it would require a change of 20° C to cause a change of $0 \cdot 02\%$ in reference.

During these tests, and also on the final arrangement at site, the entire tachogenerator was enclosed in a heat-insulating box, with a heater and thermostatic control. An internal fan on the tachogenerator shaft circulates the warm air through louvred end plates.

The tachogenerator is mounted on a separate plinth at a small distance from the main gearbox, in order to avoid exposing it to an additional source of heat. The plinth is mounted on rubber blocks to prevent any mechanical vibration present on the motor room floor affecting the magnetic flux in the tachogenerator, since, if the excitation current has recently been altered, sharply jarring the tachogenerator causes an instantaneous permanent change of magnetic flux. Repeated jarrings produce smaller changes until eventually a steady-state flux is reached.

The effects of temperature changes on the tacho-rectifier and stabilized voltage reference were studied in detail. There is a combined effect giving a change of 0·027% per deg C in speed reference. Temperature changes of the rectifier alone produce a maximum drift in speed reference of about 0·07% per deg C. Although these results are not conclusive, it appears that temperature effects are partially compensating.

The voltage-reference valves in the stabilized supply—two type QS83/3—have a temperature coefficient of voltage of 0·003 3% per deg C. This drift in reference voltage causes a corresponding change in tachogenerator field current, which is largely compensated by the circuit arrangement. This avoids the necessity of having to control the temperature of the voltage reference supply.

The tacho-rectifiers and the smoothing circuit are built into a totally enclosed steel box with a heater and thermostatic control. The heater and thermostat operate with a temperature differential of approximately $\pm\frac{1}{2}$° C. The normal operating temperature is about 35° C. A small circulating fan of about 5–10 watts mounted inside the box is sufficient to maintain a uniform air temperature.

The multi-turn reference potentiometer is liable to resistance changes due to temperature changes inside the control desk. It is therefore mounted in a temperature-controlled steel box, with a circulating fan to keep the air temperature uniform irrespective of the thermal dissipation from the top surface of the desk.

c_3 = Generator gain, volts per field ampere.
R_3 = Generator field circuit resistance, ohms.
v_x = 'Follow-on' generator output, volts.
c_x = 'Follow-on' generator gain, volts per field ampere.
R_x = 'Follow-on' generator field-circuit resistance, ohms.
k_0 = Flux constant of pilot motor, volts per rad/s.
$\hat{\omega}$ = Pilot motor speed, rad/s.
i_0 = Pilot-motor armature current, amp.
R_0 = Pilot-motor armature circuit resistance, ohms.
M_0 = Load torque on pilot motor due to rheostat, lb-ft.
J_0 = Inertia of pilot motor and rheostat, slugs-ft^2.
ρ = Angular movement of rheostat, rad.
K_1 = Rheostat constant, volts/rad.
v = Voltage output from rheostat, volts.
v_2'' = Variable-voltage-exciter output, volts. (Resulting from 'follow-on' circuit.)
c_4 = Variable-voltage-exciter gain, volts per field ampere. (Resulting from 'follow-on' circuit.)
R_4 = Variable-voltage-exciter field circuit resistance, ohms. (From 'follow-on' circuit.)
i_1 = D.C. motor armature current, amp.
R_1 = D.C. armature circuit resistance, ohms.
k_1 = Flux constant of d.c. motor, volts per rad/s.
M_1 = Compressor load torque, referred to d.c. motor, lb-ft.
J_1 = Total inertia referred to d.c. motor shaft, slugs-ft^2.
k_f = Gain of feedback network.
τ_1 = Time-constant of tacho-smoothing circuit, sec.

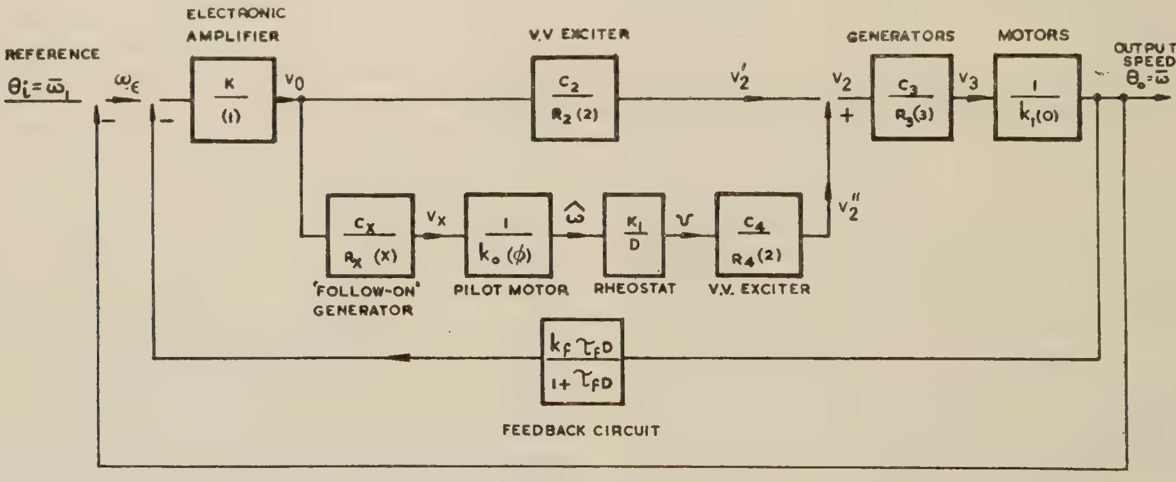

Fig. 15.—Block schematic of d.c. motor-speed control, without current-limit control.

(9) MATHEMATICAL ANALYSIS

The following symbols are used and the appropriate units are given. The block schematic of the system is shown in Fig. 15.

ω_ϵ = Deviation applied to the electronic amplifier unit, rad/s.
$\bar{\omega}$ = D.C. motor speed, rad/s.
$\bar{\omega}_1$ = Set speed, rad/s.
K = Electronic amplifier gain, volts per rad/s.
v_0 = Electronic amplifier output, volts.
v_2' = Variable-voltage exciter output, volts. (Resulting from electronic amplifier.)
c_2 = Variable-voltage-exciter gain, volts per field ampere. (Resulting from electronic amplifier.)
R_2 = Variable-voltage-exciter field-circuit resistance, ohms. (From electronic amplifier.)
v_2 = Variable-voltage-exciter total output, volts.
v_3 = Generator output, volts.
τ_2 = Effective time-constant of variable-voltage exciter, sec.
τ_3 = Generator field time-constant, sec.
τ_0 = Mechanical time-constant of d.c. motor, sec. [Defined in eqn. (12*a*).]
τ_ϕ = Mechanical time-constant of pilot motor, sec. [Defined in eqn. (14*a*).]
τ_x = 'Follow-on' generator field time-constant, sec.
τ_f = Time-constant of feedback network, sec.
ω = Angular frequency, rad/s.
D = Differential operator.
X_1 = Constant, defined in eqn. (12*b*).
X_0 = Constant, defined in eqn. (14*b*).
m_1 = Constant, defined in eqn. (15).
m_2 = Constant, defined in eqn. (16).
y = Ratio, m_2/m_1.

For brevity, the expressions $(1 + \tau_1 D)$, $(1 + \tau_0 D)$, etc., have been shortened to (1), (0), etc.

(9.1) Equations

Electronic output: $K(\bar{\omega}_1 - \bar{\omega}) = v_0(1)$ . . . . (1)

Exciter and generator:
$$\begin{cases} c_2 v_0 = R_2(2)v_2' & (2) \\ c_3 v_2 = R_3(3)v_3 & (3) \\ v_2 = v_2' + v_2'' & (4) \end{cases}$$

'Follow-on' control:
$$\begin{cases} c_x v_0 = R_x(x)v_x & (5) \\ v_x = k_0\hat{\omega} + i_0 R_0 & (6) \\ 0{\cdot}74k_0 i_0 = M_0 + J_0 D\hat{\omega} & (7) \\ \hat{\omega} = D\rho & (8) \\ K_1\rho = v & (9) \\ vc_4 = R_4(2)v_2'' & (10) \end{cases}$$

It is assumed that movement of the pilot motor changes only the voltage applied to VVEF1, i.e. the change in resistance R_4 is neglected.

The effective time-constant τ_2 applies to transients in the field windings on the variable-voltage exciter, i.e. the arithmetic sum of the individual time-constants of each field winding.

Generator voltage and output speed
$$\begin{cases} v_3 = i_1 R_1 + k_1\bar{\omega} & (11) \\ 0{\cdot}74k_1 i_1 = M_1 + J_1 D\bar{\omega} & (12) \end{cases}$$

From eqns. (11) and (12), eliminating i_1,

$$v_3 = \frac{R_1 M_1}{0{\cdot}74k_1} + (1 + \tau_0 D)k_1\bar{\omega}$$

where, by definition,

$$\tau_0 = \frac{J_1 R_1}{0{\cdot}74k_1^2} \quad \text{(12a)}$$

i.e. $v_3 = X_1 + (0)k_1\bar{\omega}$

where
$$X_1 = \frac{R_1 M_1}{0{\cdot}74k_1} \quad \text{(12b)}$$

From eqn. (3), eliminating v_3,

$$c_3 v_2 = R_3(3)X_1 + R_3(3)(0)k_1\bar{\omega} \quad \text{(13)}$$

From eqns. (6) and (7), eliminating i_0,

$$v_x = X_0 + (\phi)k_0\hat{\omega} \quad \text{(14)}$$

where, by definition,

$$\tau_\phi = \frac{J_0 R_0}{0{\cdot}74k_0^2} \quad \text{(14a)}$$

and
$$X_0 = \frac{R_0 M_0}{0{\cdot}74k_0} \quad \text{(14b)}$$

By letting
$$m_1 = \frac{Kc_2c_3}{k_1R_2R_3} \quad \text{(15)}$$

and
$$m_2 = \frac{Kc_xK_1c_3c_4}{k_0R_xk_1R_3R_4} \quad \text{(16)}$$

and eliminating all variables except the output speed $\bar{\omega}$, we have

$$[m_1(\phi)(x)D + m_2]\bar{\omega}_1 - \frac{m_2R_x}{Kc_x}(1)(x)X_0 - \frac{(1)(2)(3)(\phi)(x)DX_1}{k_1}$$
$$= [(0)(1)(2)(3)(\phi)(x)D + m_1(\phi)(x)D + m_2]\bar{\omega} \quad \text{(17)}$$

(9.2) Stability

For a stability check, we are interested in the roots of the differential expression on the right-hand side of eqn. (17). However, the system stability is most readily obtained by applying Nyquist's criterion.

A plot of $\omega_\epsilon/\bar{\omega}$ is the inverse Nyquist diagram.

Since we require the relationship between error and output only, the external torque terms, involving constants X_0 and X_1, may be ignored;

i.e. $[m_1(\phi)(x)D + m_2]\bar{\omega}_1 = [(0)(1)(2)(3)(\phi)(x)D]\bar{\omega} + [m_1(\phi)(x)D + m_2]\bar{\omega}$

as $\bar{\omega}_1 - \bar{\omega} = \omega_\epsilon$ and $\bar{\omega} = \theta_0$

then,
$$\omega_\epsilon/\theta_0 = \frac{(0)(1)(2)(3)(\phi)(x)D}{m_1(\phi)(x)D + m_2} \quad \text{(18)}$$

Examination of this expression for ω_ϵ/θ_0 shows that a relationship between m_1 and m_2 is required.

m_1 is a constant gain for a class 0 system. m_2 is adjustable, since it depends on the value of K_1, the rate of change of v with time. Hence, in order to determine m_2 a time scale is necessary. If we let

$$m_2 = m_1 y \quad \text{i.e. } y = \frac{m_2}{m_1} = \frac{K_1R_2c_4c_x}{k_0c_2R_4R_x}$$

then
$$\omega_\epsilon/\theta_0 = \frac{(0)(1)(2)(3)(\phi)(x)D}{m_1[y + (\phi)(x)D]}$$

To determine the value of y, we have, from eqns. (8) and (9),

$$\frac{d\rho}{dt} = \hat{\omega} \text{ and } \rho = \frac{v}{K_1}$$

i.e. after t_1 seconds, $\rho = \hat{\omega}t_1 = \dfrac{v}{K_1}$

If, after time t_1, the exciter output voltage increases by an amount equivalent to the maximum electronic-amplifier field output, then, under steady-state conditions:

$$\frac{c_2}{R_2}v_0 = \frac{c_4}{R_4}v, \quad \text{i.e. } v = \frac{R_4c_2}{c_4R_2}v_0$$

i.e.
$$\hat{\omega}t_1 = \frac{R_4c_2v_0}{c_4R_2K_1}, \quad \text{i.e. } \frac{R_2K_1c_4}{c_2R_4} = \frac{v_0}{\hat{\omega}t_1}$$

Now,
$$v_0\frac{c_x}{R_x} = v_x \simeq k_0\hat{\omega}$$

i.e.
$$v_0 \simeq \frac{R_x}{c_x}k_0\hat{\omega}$$

i.e.
$$\frac{R_2K_1c_4}{c_2R_4}\,\frac{c_x}{k_0R_x} = \frac{1}{t_1} = y$$

i.e.
$$m_2 = m_1 \times \frac{1}{t_1}$$

Further consideration of ω_ϵ/θ_0 shows that the expression is composed of two factors, multiplied together,

$$\frac{(0)(1)(2)(3)}{m_1}$$

which is the factor for 'proportional control',

and
$$\frac{(\phi)(x)D}{y + (\phi)(x)D}$$

which is the multiplying factor for 'integral control'. Having simplified the expression for ω_ϵ/θ_0, the effect of 'integral control'

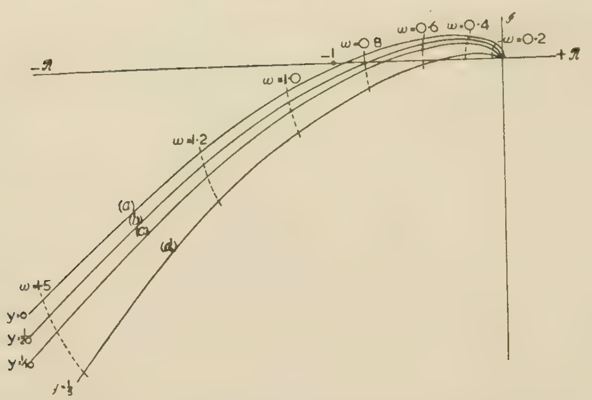

Fig. 16.—Inverse Nyquist diagram for 'proportional' and 'integral' control.

may be determined from a plot of eqn. (18). Referring to Fig. 16, the inverse Nyquist diagrams have been drawn for

(*a*) Proportional control alone.
(*b*) $t_1 = 20$ sec.
(*c*) $t_1 = 10$ sec.
(*d*) $t_1 = 5$ sec.

The compressor is designed to work with either four or ten stages and the mechanical time-constant τ_0 has two values, namely, 6 and 13 sec, respectively. The higher value has been taken for purposes of this analysis.

It can be seen that, when $t_1 = 20$ sec, the effect of the 'integral control' on the normal forward loop is relatively small. Further, the maximum electronic-amplifier field output is designed to be 10% of the total. The time required for 100% output from the exciter due to VVEF1 is 200 sec. This is comparable with the total accelerating time, of approximately 10 min, under gas-turbine control.

Since, under operational conditions, the ideal control behaves as an 'integral control', i.e. zero-error system, the determination of gain is not straightforward. However, if we consider the system as purely 'proportional control', the system gain is fixed by the ratio of the natural regulation of the Ward Leonard drive to the required regulation under speed control, i.e. say 4 to 0·1%.

Therefore the system gain m_1 is 40.

(9.3) Feedback

To give a suitable stable response for a loop gain of 40, an overall transient feedback with a transfer function

$$\frac{k_f \tau_f D}{1 + \tau_f D}$$

was used,

where k_f = Feedback gain of the tachogenerator driven at $\bar{\omega}$ radians per second, volts per rad/s.

and τ_f = Time-constant in a *CR* network.

The values are as follows: $\tau_f = 0{\cdot}25$ sec

(*a*) $k_f = 5{\cdot}0$ volts per rad/s

or (*b*) $k_f = 6{\cdot}0$ volts per rad/s.

The feedback gain may be adjusted by means of a tapped voltage transformer in the tachogenerator output circuit.

(9.4) Stability with Feedback

Fig. 17 shows, in curve (*a*), the Q-diagram for the time-constants given in Table 1. This curve crosses the negative real axis at a point corresponding to a system gain of 33, which is the wrong side of the $(-1, j0)$ point, showing that the system by itself would be unstable. Curve (*b*) shows the addition of the feedback vectors on the original locus, with a feedback gain of 5 volts per rad/s. This new locus is tangential to the $M = 1{\cdot}14$ circle, so that we may expect a step input of reference to produce a stable speed change with approximately $(1{\cdot}14 - 1{\cdot}0)/1{\cdot}14 \simeq 12\%$ first overshoot. The value of ω at the $M = 1{\cdot}14$ circle is approximately 1·25, which we would expect to be the natural oscillation frequency of the system. The higher value of ω at the $M = 1$ circle is approximately 1·4, so that the expected time to cross-over would be $\pi/1{\cdot}4 = 2\frac{1}{4}$ sec. Also the radius of the $M = 1{\cdot}14$ circle laid along the locus from $\omega = 1{\cdot}0$ to $1{\cdot}4$ indicates that we may expect an exponential decrement of 0·4.

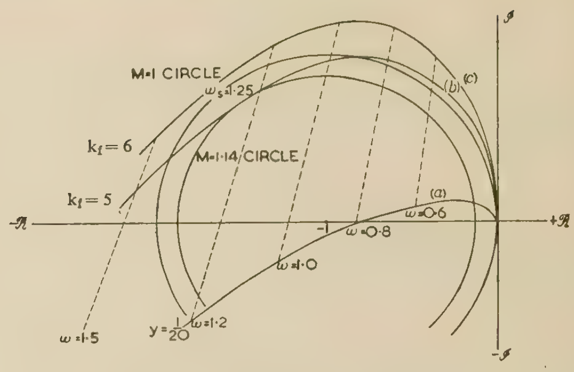

Fig. 17.—Q-diagram with feedback.

Table 1

The time-constants are as follows:

$\tau_0 = 13$ sec
$\tau_1 = 0{\cdot}1$ sec
$\tau_2 = 0{\cdot}4$ sec
$\tau_3 = 3{\cdot}5$ sec
$\tau_\phi = 0{\cdot}015$ sec
$\tau_x = 0{\cdot}08$ sec

Increasing the feedback gain to 6 volts per rad/s should produce an overdamped system as shown in curve (*c*), the locus lying entirely outside the $M = 1$ circle.

Although the results obtained from the Q-diagram for time to cross-over, oscillation frequency, first overshoot and exponential decrement are only approximate (the general theory of 'curvilinear squares' applies only to linear systems with two time lags), they are sufficiently encouraging to proceed with the transient analysis on the basis of the single feedback quantity.

(10) CLOSED-LOOP TRANSIENT RESPONSE

(10.1) Equations

Since the inverse Nyquist plot with $y = 1/20$ is approximately the same as with $y = 0$, the response calculations have been simplified, and the equation representing the proportional system, including feedback, is as follows:

$$\omega_\epsilon/\bar{\omega} = \frac{(0)(1)(2)(3)}{m_1} + \frac{k_f \tau_f D}{1 + \tau_f D} \quad . \quad . \quad . \quad (19)$$

i.e. the complete equation may be written as

$$\bar{\omega}_1[m_1(f)] - \bar{\omega}[(0)(1)(2)(3)(f) + m_1 k_f \tau_f D + m_1(f)] \quad . \quad (20)$$

Responses are calculated for a step function of $\bar{\omega}_1$.

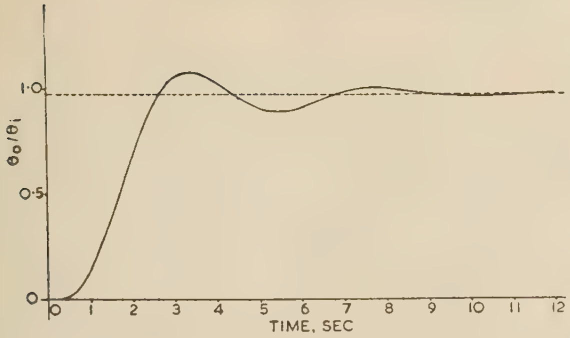

Fig. 18.—Calculated response to step input of reference.

The solution obtained by substituting the appropriate values in eqn. (20) is as follows (neglecting minor terms):

$$\frac{\bar{\omega}}{\bar{\omega}_1} = 0\cdot978 - 0\cdot975\varepsilon^{-0\cdot81t} - 0\cdot59\varepsilon^{-0\cdot38t}\sin(1\cdot377t) \quad (21)$$

This has been plotted in Fig. 18, from which it may be seen that the overshoot is 10%, with a time to cross-over of 2·7 sec and a settling time of 10 sec, for a small step of reference.

(10.2) Analogue-Computer Results

Fig. 19(*b*) shows the output response of the linear system with a step of input reference. The feedback gain is $k_f = 5$ volts per rad/s. The similarity between Figs. 18 and 19(*b*) is encouraging, the calculated result showing the analogue simulation to be correct.

This and the following analogue-computer results include the effect due to the 'integral control', whereas the simplified calculated result omitted the 'integral control' as having negligible effect.

Figs. 19(*a*), 19(*c*), and 19(*d*) show the variation in response with feedback gain. The values of k_f are 4·5, 6·0 and 7·0 volts per rad/s, respectively. The last value gives an overdamped system, which is undesirable, as it will have a long settling time compared with the previous values of k_f. Fig. 19(*d*) cannot show this characteristic very clearly, as the deviation is very small compared with the full-scale deflection.

So far, the response of the system has been studed with regard to a step of reference voltage. Another important response is that due to a disturbance in the speed-control system resulting from a change in load torque, i.e. compressor torque. This may be due to a change in air density, or the incidence of the model affecting the velocity of the air in the tunnel.

Fig. 20 shows the speed response of the linear system to a step change in load torque. In a 'proportional control' system a full-load torque change would result in a steady-state speed change of 0·1%, i.e. the load regulation of the closed-loop system.

(10.3) Non-Linearities

In a speed-control system such as the one described, there are

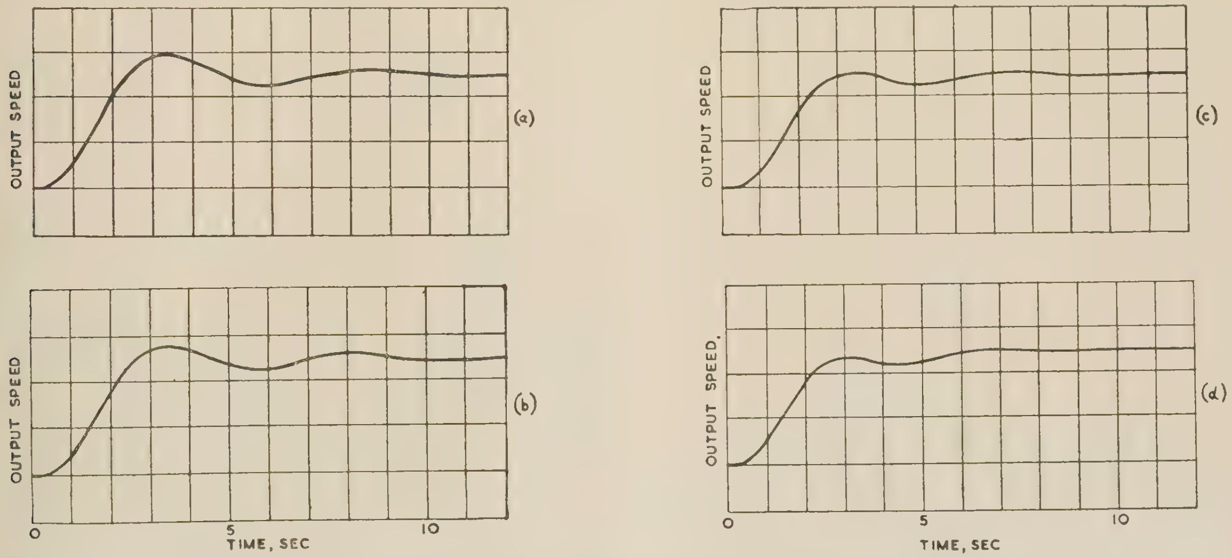

Fig. 19.—Transient response to step input of reference, obtained from analogue computer.

(*a*) $k_f = 4\cdot5$. (*c*) $k_f = 6\cdot0$.
(*b*) $k_f = 5\cdot0$. (*d*) $k_f = 7\cdot0$.

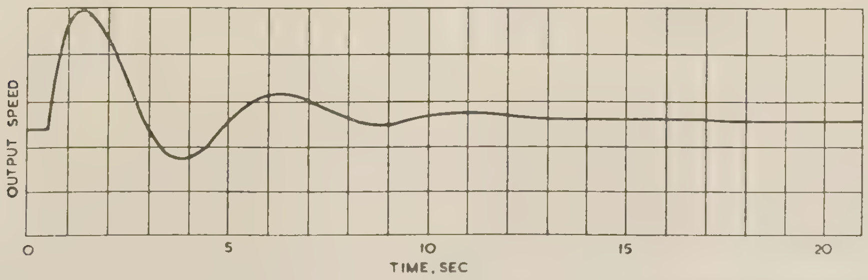

Fig. 20.—Transient response to step of load torque, obtained from analogue computer.

two major non-linearities apart from a number of minor ones that are normally neglected.

The first non-linear characteristic that appears with an increase in error signal is amplifier saturation. This causes a reduction in system gain of 100 to 200 times. The response of such a system is grossly over-damped as shown in Figs. 21 and 22, where

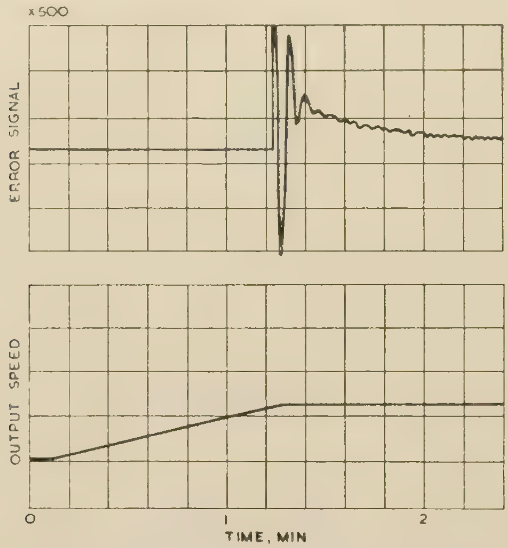

Fig. 21.—Acceleration with electronic amplifier saturated, obtained from analogue computer.

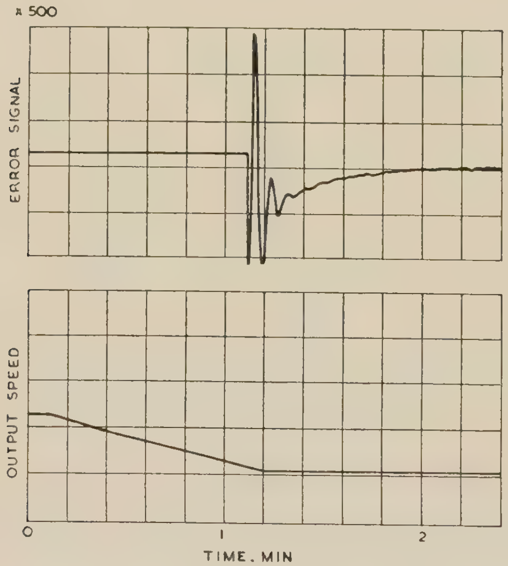

Fig. 22.—Deceleration with electronic amplifier saturated, obtained from analogue computer.

the error-signal characteristic is plotted with a vertical scale factor of 500 times. These figures show the long settling time associated with 'integral control'.

The second major non-linearity is due to the current-limiting feature in the main Ward Leonard loop. This is designed to keep the d.c. machines within their transient over-current capacity however large the system error. In other words, the correcting torque has to be limited to safeguard the d.c. motors and generators. The current-limit control forms a separate servo mechanism which must be analysed and stabilized independently.

(11) ACKNOWLEDGMENT

The authors wish to express their appreciation of the encouragement given in the preparation of this paper by the Chief Engineer (Stafford) The English Electric Co., Ltd., and the Chief Engineer (M. and E.) Ministry of Works, and for the permission to publish given by the Director, Royal Aircraft Establishment.

Much of the work described in the paper is based on The English Electric Co. designs, and the authors are glad to acknowledge the assistance given by colleagues, in particular to Mr. T. Coxon for work in connection with the mathematical analysis and Mr. T. Lipinski for his investigations into the high-frequency tachogenerator.

(12) APPENDIX

Actual Results

Measurements were taken on the main drive speed-control equipment of the 8 ft wind tunnel in October, 1957. For this series of tests the h.p. compressor was out of circuit and the dummy section was in position. The tunnel was evacuated in order to allow the d.c. motors driving the 4-stage low-pressure compressor to run at full speed. The main 68 000 h.p. a.c. motor was not energized during the speed-holding tests.

A very sensitive measurement of speed was made using a crystal-gated counter to integrate speed over a time interval of 10 sec. The counter signal was obtained from a photo-electric pick-up, electronically amplified to about 20 volts (peak). The light source was interrupted by 600 steel pegs driven into the face of the brake wheel on the main compressor shaft, so that, at a speed of 750 r.p.m., the total count was 75 000. By this means, a possible speed change of 0·1% at this speed is represented by a change of 75 in the count.

(12.1) Long-Term Stability Test

The long-term stability run was to enable the drift levels in the complete equipment to be determined. The d.c. drive was run under automatic speed control at a fixed set speed of approximately 600 r.p.m. for 5 hours' duration. The first reading was taken ¼ hour after setting the speed. For the first hour of the test, readings of speed, load and various temperatures were taken every 5 min and thereafter every 15 min until the completion of the test. For the 5 min readings, single 10 sec counts were taken, but subsequently, for the 15 min readings, the average of ten 10 sec counts were taken for the speed reading.

The temperatures inside the tachogenerator box, tacho-rectifier box and speed-reference potentiometer were controlled and did not vary by more than $\pm 0 \cdot 5°$ C during the whole test. The d.c. load increased gradually and steadily during the first 3 hours of the test from 3 050 to 3 700 amp, owing to a steady increase in the air pressure within the tunnel. After 3 hours the tunnel was further evacuated until the d.c. load was reduced to 2 700 amp, and subsequently for the remaining 2 hours of the test, the d.c. load steadily rose to 3 150 amp.

The 3-phase 50 c/s a.c. supply voltage was carefully metered during the test, and random voltage changes amounted to a total variation of 410–430 volts.

The speed measurements are shown in Fig. 23, together with the specified limits of speed accuracy. It will be seen that the control responded to the d.c. load change that occurred after 3 hours.

(12.2) Short-Term Stability Tests

Short-term stability tests consisted of a number of runs of 30 min duration designed to determine the short-term stability

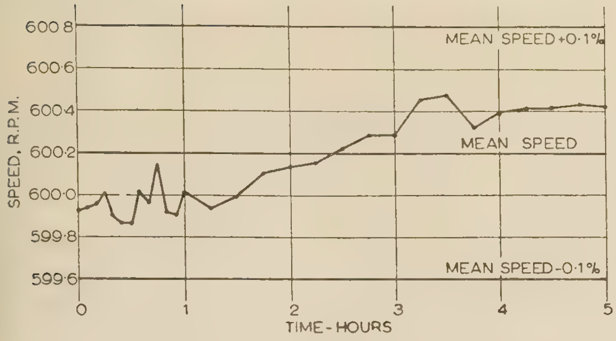

Fig. 23.—Speed record of long-term stability test.

of the control system. The d.c. drive was run under automatic speed control at fixed set speeds of approximately 300, 150, and 75 r.p.m. Again, the first reading in each test was taken ¼ hour after setting the speed. Readings of speed, load and temperatures were taken every 5 min. Speed readings were the average of ten 10 sec counts. Temperature variations in the air temperature inside the thermostatically controlled boxes were less than $\pm 0{\cdot}5^\circ$C. Fig. 24 shows the speed measurements taken at each of the three set speeds. The d.c. load current remained substantially constant during each 30 min run and was metered as follows:

1 050 amp at 300 r.p.m.
1 550 amp at 150 r.p.m.
500 amp at 75 r.p.m.

The last two readings were with the tunnel at atmospheric air pressure.

(12.3) D.C. Load Change. Step-Off

The test of changing load was by suddenly reducing the d.c. load, with the d.c. drive running under automatic speed control at a set speed of 318 r.p.m., with the tunnel evacuated to 3·4 in Hg static pressure. This was achieved by using the 68 000 h.p. a.c. motor as a generator supplying a test-load tank, and by opening the circuit-breaker to reduce the d.c. load suddenly. Initially the a.c. motor excitation power was adjusted to give 5 000 amp d.c. load. After tripping the a.c. circuit-breaker the base load supplied to the compressor was 1 000 amp d.c., i.e. a step of load corresponding to two-thirds full-load torque of the d.c. drive. Fig. 25 shows the transient speed change, to this step of load torque, taken with a high-speed pen recorder fed from the rectified tachogenerator voltage.

(12.4) Conclusions

Both the long-term and the short-term stability tests show that, under the load conditions prevailing with the low-pressure compressor, the speed-control system can maintain the speed to an accuracy within less than $\pm 0{\cdot}1\%$ of set speed, from full speed down to one-tenth of full speed.

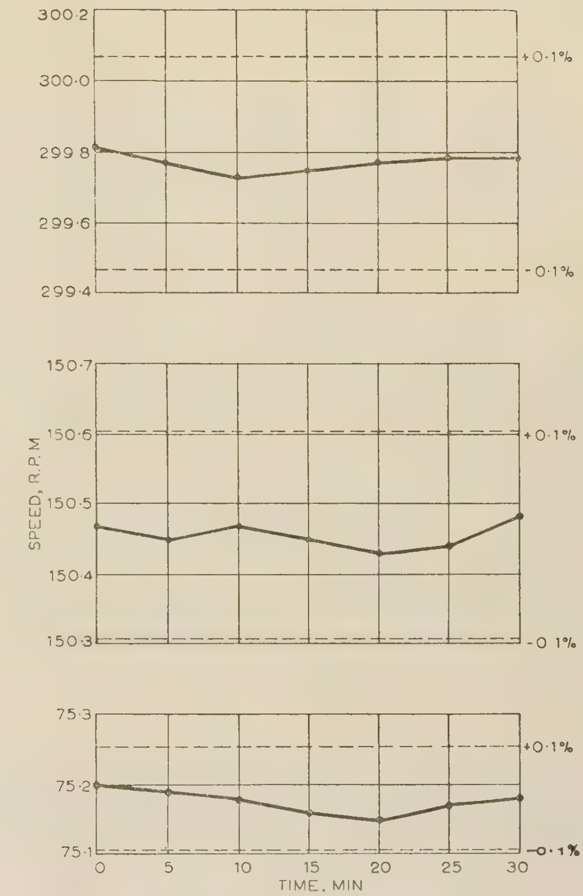

Fig. 24.—Speed record of short-term stability tests.

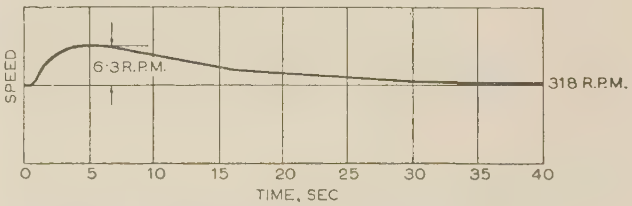

Fig. 25.—Transient speed change to step of load torque.

The transient performance resulting from a large step of load torque at 318 r.p.m. shows a 2% peak speed disturbance for a two-thirds full-load torque step 'off' and a recovery time of approximately 35 sec under the direct action of the 'follow-on' control.

[The discussion on the above paper will be found on page 228.]

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AUTOMATIC SETTING OF THE FLEXIBLE WALLS OF A LARGE WIND TUNNEL

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SUMMARY

To develop supersonic air flow in a wind tunnel it is usual to provide a nicely contoured constriction in the air passage ahead of the model test region, and it is necessary to alter the cross-section of this 'throat' to cater for different air speeds. In the tunnel under consideration, the constriction is formed by two flexible steel plates, sliding between side walls and constrained to the required adjustable profile by a series of hydraulically-actuated screw jacks.

The paper deals with the electrical control system, for storing the necessary jack-setting data on punched tape, and releasing it to govern, simultaneously, the motion of every screw jack with the required degree of precision, so that the shape of the aerodynamic throat can be adjusted progressively throughout the air-speed range of the tunnel.

In developing the system, a number of pieces of electro-mechanical equipment had to be devised. Some of this new apparatus is briefly described, and its operation is indicated.

(1) INTRODUCTION

The paper is concerned with the light-current control system which has been provided for controlling the air speed, in the supersonic range, of the 8 ft × 8 ft high-speed wind tunnel at the Royal Aircraft Establishment, Bedford.

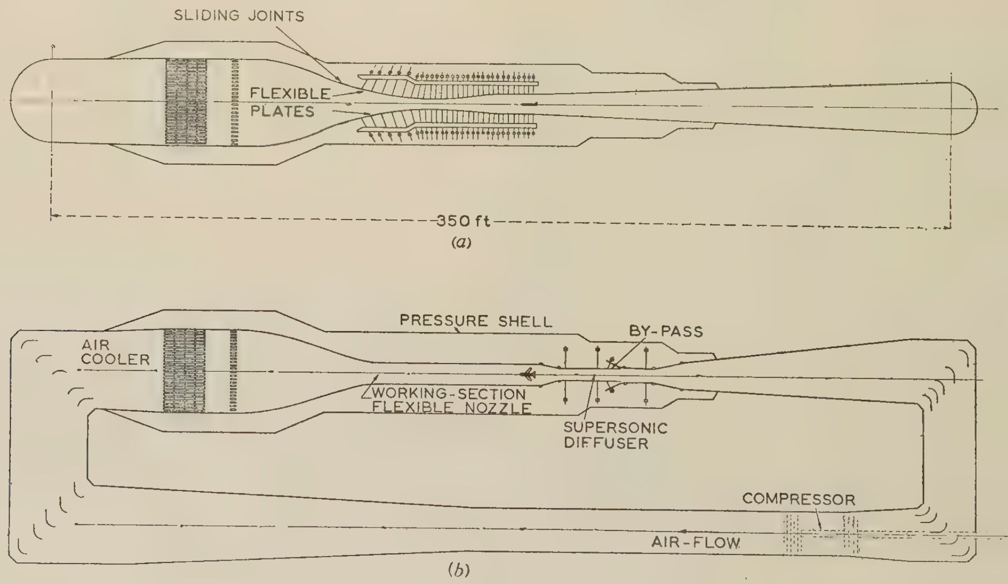


Fig. 1.—Outline of 8 ft × 8 ft wind tunnel.

(*a*) Elevation of working section leg.
(*b*) Plan.

This tunnel, as outlined in Fig. 1, is of the return-flow type and is designed for operation both in the subsonic region, and in the supersonic region up to a Mach number of 2·8 (i.e. 2·8 times the speed of sound). The essential features shown in Fig. 1 are as follows:

(*a*) A pressure shell to contain the air flow.
(*b*) A compressor to circulate the air.
(*c*) A working-section nozzle where the air speed may be controlled.
(*d*) A supersonic diffuser for achieving maximum energy recovery.

For speeds below that of sound the velocity of the air in the test region is adjusted by the speed, and therefore the output, of the compressor. Under this condition, the working-section nozzle takes the form of an entry flare, followed by a test region of sensibly uniform cross-section [see Fig. 2(*a*)]. However, when the speed of sound is approached, shock waves accumulate at the flare entry, and further increase by raising the compressor output is not practicable. To develop supersonic flow, a constriction of the air passage upstream of the model is required, as shown in Fig. 2(*b*). Then the air speed around the model is determined by the relative sectional areas of the constriction and of the tunnel in the test region, and no longer by the compressor output. The latter, however, must be sufficient to force the shock waves downstream of the model.

It has been the practice in a number of tunnels to form the constriction by shaping laminated timber blocks to the required profile, in which case a different set of blocks is required for each Mach number. For a large wind tunnel, changing the air speed by this method would be a very slow process involving stopping the compressor, equalizing the tunnel pressure to the atmosphere, changing the blocks (weighing perhaps 30 tons each), adjusting

The paper is a communication from the Staff of the Research Laboratories of The General Electric Company, Limited, Wembley, England.
Mr. Barnes is at the Ministry of Works.

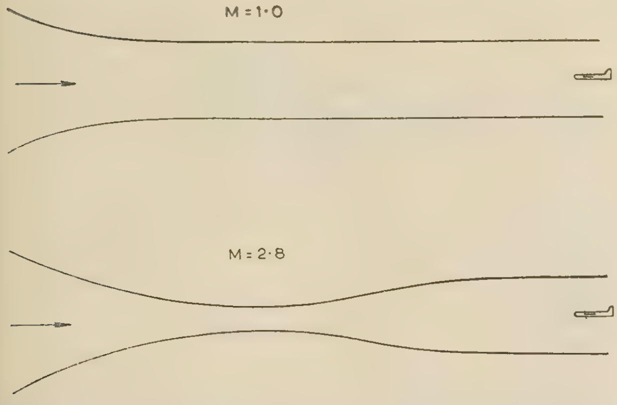


Fig. 2.—Nozzle shapes for subsonic and supersonic air flow.

the pressure and restarting the compressor. It will be apparent that there are advantages in an alternative method in which the walls of the air passage are flexible and can be adjusted to the required shape whilst the tunnel is running. Much time (and a considerable amount of electrical energy) is saved, any intermediate Mach number within the range of the tunnel may be achieved, and there is not the need for providing (and storing) a considerable range of blocks. Several tunnels having flexible nozzles are in existence, and they show considerable variation in the systems adopted for adjusting the walls.

In large supersonic tunnels, where considerable power is involved, it is advantageous to provide a second constriction—a supersonic diffuser—downstream of the model. By proper adjustment of its shape in relation to the Mach number, it is possible to set up conditions suitable for the maximum recovery of potential energy from the kinetic energy of the air stream; thereby a considerable reduction in the rating of the compressor plant can be achieved.

The control system under consideration provides for the simultaneous and automatic setting of both the working-section nozzle and the supersonic diffuser, for any selected Mach number.

(2) RÉSUMÉ OF REQUIREMENTS

In the working-section nozzle two flexible steel plates make the upper and lower air-swept surfaces, and they slide between two fixed side walls, as indicated in Fig. 1. The flexible plates are approximately 65 ft long, 8 ft wide, and 1 in thick, and are constrained to the required shape by 30 jacking stations distributed along their length. Each jacking point consists of two 30-ton screw jacks, supported in a portal frame structure built up on the side walls and driven by a common hydraulic motor. Fig. 3 is a sketch of the arrangement at a station for the upper flexible plate.

The theoretical nozzle shape can be calculated for any air speed, and the control system must be capable of storing this information and then directing the 60 pairs of screw jacks to bend the plates to the required shape whenever that particular Mach number is called for. Moreover, great precision is required; first, because the quality of the air flow in the model region is critically dependent on exact wall curvature, and secondly, because the stress developed in bending the plates, even to the correct profile, is relatively high. Should any jack lead or lag during the reshaping process by more than a very small amount, the stress may reach the yield point. In brief, for safety and for quality of the air flow, every jack must be kept at all times within $\pm 0{\cdot}010$ in of its calculated position, and the development of the control system was based upon a target accuracy of $\pm 0{\cdot}005$ in.

There are a number of reasons why the theoretically calculated wall shape for a given Mach number may require correction for measured imperfections of the air flow. Means must therefore be built into the system whereby a small manual adjustment can be fed into each station, and the amount of it indicated on the control panel. It is expected that this facility will be most used in the early stages; when sufficient experience has been obtained, it should be possible to assimilate these adjustments in the automatic working by supplying a new set of punched tapes.

(3) METHOD OF AUTOMATIC CONTROL

After a number of alternatives had been considered, a scheme, proposed by the Ministry of Supply, was adopted involving step-by-step adjustment of the jacks under control of data stored on punched tape.

A pilot lead-screw of precision grade is fitted alongside each pair of screw jacks (see Fig. 3), and this is retracted or extended in steps of fixed amount by an actuator (termed an 'impulse receiving unit') fed with electrical signals from outside the pressure shell in accordance with perforations on the control tape for that station. Thus, at every instant, the pilot screw is made to set up the required jack extension, and the main screw jacks are hydraulically driven to follow the pilot screw. This is arranged by siting the hydraulic control valve so that its piston is actuated by any transient difference between the extensions of pilot and jack.

A separate punched tape has been provided for each jacking station, and, when all the tapes are run through their readers together, the nozzle shape varies in a way predetermined by the tape perforations and gives a progressive change of air speed from Mach $1{\cdot}0$ to Mach $2{\cdot}8$. For the stations near the supersonic throat, where the total jack movement is greatest (34 in), each perforation represents $0{\cdot}0025$ in, so that 13 600 perforations are required, and this determines the physical length of all the tapes. Tapes for other stations have fewer holes more widely spaced. Near the model the total jack movement is small, and precision of wall shape is more important. Consequently for nine stations a reduced step of $0{\cdot}00125$ in has been adopted.

A uniform rate of impulsing of $6{\cdot}5$ per second is used, giving a programme time of 40 min (approximately) for covering the complete air-speed range. A faster rate might be desirable from the operational standpoint, but there is a limit set by the amount of hydraulic power available.

Errors, either resulting from failures of apparatus to respond to every impulse or due to missing or false impulses, are not tolerable, especially should they accumulate. Not only will the wall get out of shape, but its actual shape will become unknown to the operating staff. This danger has been guarded against by providing a second punched tape for each station. This operates a monitoring receiver and checks the action of the impulse receiving unit at every step. An error so detected can usually be rectified, and automatic action resumed, without the need for entering the tunnel.

(4) DESCRIPTION OF CONTROL SYSTEM FOR THE WORKING SECTION

(4.1) Schematic

Fig. 4 is a block schematic illustrating the general principles of the method of control evolved to meet the requirements discussed in the previous Section. This Figure indicates the apparatus for one jacking station on the upper wall of the working section and the corresponding station on the lower wall. Briefly,

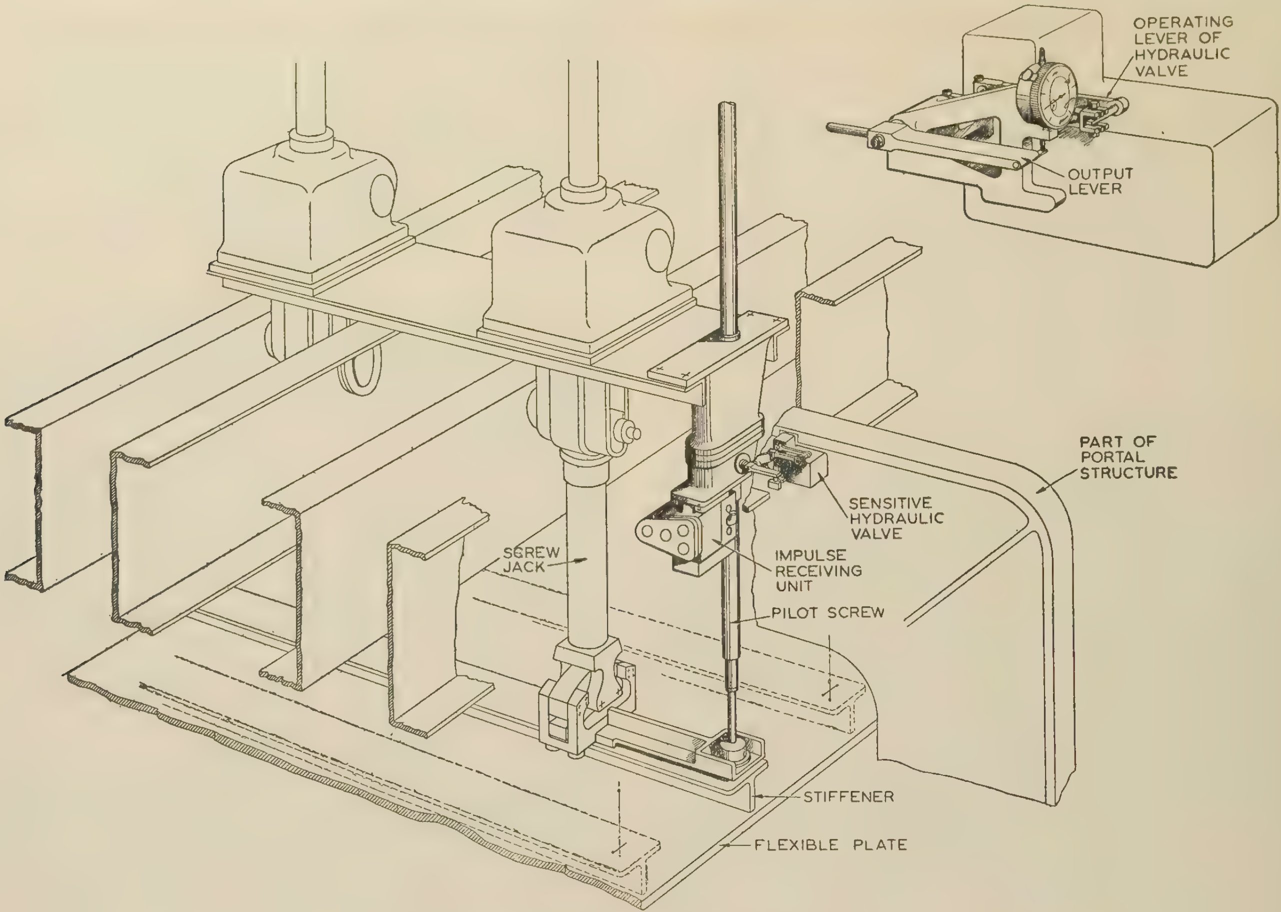


Fig. 3.—Sketch of a jacking station on the upper wall.

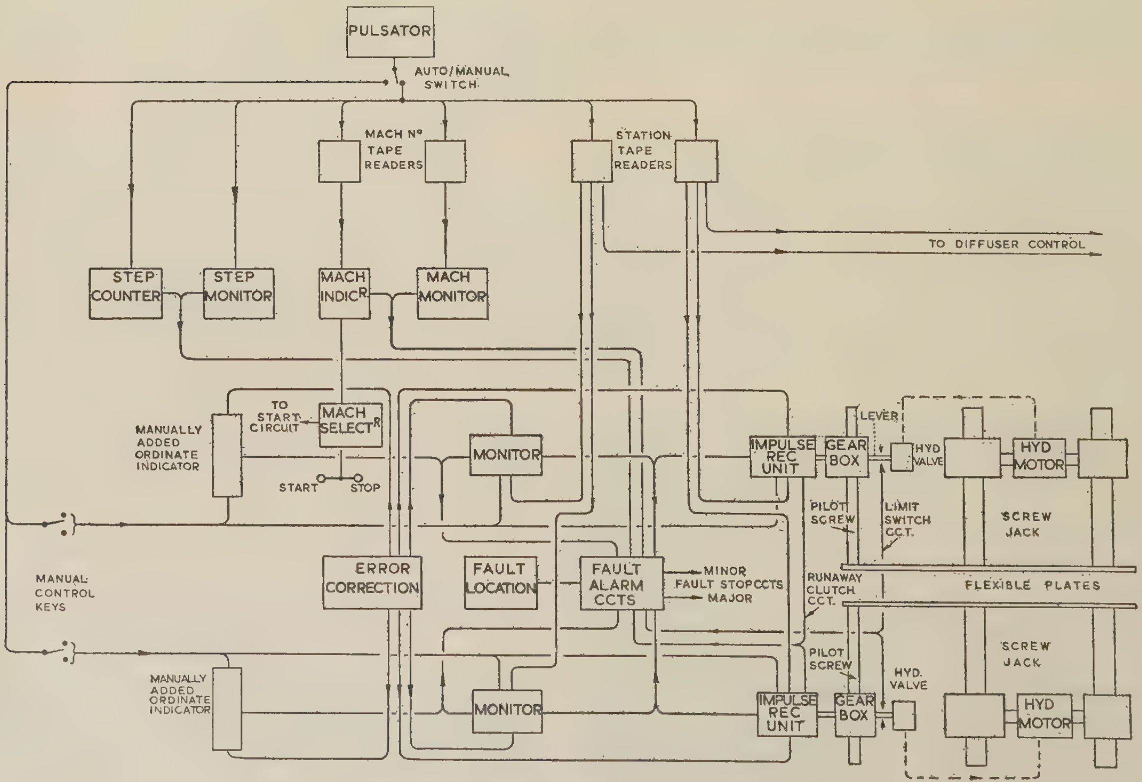


Fig. 4.—Schematic of control system for the working section.

he operation of this complex system is governed by a primary npulse machine—the pulsator—which, when a new Mach umber has been selected by the operator, sends a train of npulses to every tape reader until the required wall configuration as been reached. The circuits involved are of little novelty, eing in the main based on the use of the standard-pattern relay type 3000). Greater interest centres on certain items of new quipment, which had to be designed and developed. The ollowing Sections describe in detail some of these developments, nd an outline of the circuit arrangements is given to indicate heir operation.

(4.2) Punched-Tape Apparatus

4.2.1) Tape Reader.

The special requirements of a tape reader for this application, ot normally met with in telegraph working, are that it shall be apable of passing and reading tape in either direction, and that t shall deliver its output over five separate wires instead of equentially over a single wire. Since no suitable reader was vailable, it was necessary to devise modifications to an existing attern. In this instrument, which is of the start-stop pattern, he tape is fed by a pin wheel indexed by a pawl and rachet wheel, the pawl being operated from a cam on the driven member of the start-stop clutch. Instead it was found possible to provide two pawls to operate the square-toothed ratchet wheel in alternative directions. Either pawl is engaged, and the other disengaged, by a solenoid. As the reader was already fitted with five pairs of contacts operated from the peckers, it was a fairly simple matter to separate them to give 5-wire output, and opportunity was taken to readjust the timing of the peckers, so that signals of 50 millisec duration are obtained contemporaneously in place of 20 millisec sequentially.

(4.2.2) Tape Code.

As mentioned above, each step taken by a tape reader corresponds with a discrete movement of the associated jacking station, which may or may not take place according as there is, or is not, a perforation in the tape.

The five tracks on the tape are used as follows:

Track 1 lower wall—moving out (away from the centre-line of the tunnel).
Track 2 lower wall—in.
Track 3 used on eight readers only, for supersonic diffuser stations.
Track 4 upper wall—in.
Track 5 upper wall—out.

Nearly all the perforations lie in tracks 2 and 4 (not considering track 3); tracks 1 and 5 are used in comparatively rare cases where particular jacks are required to retract whilst the others are extending. When it is required to lower the Mach number, the tapes pass through the readers in the reverse direction, and the functions of tracks 1 and 2 and tracks 4 and 5 are interchanged by a reversing relay.

Preparation of the tapes has been carried out by staff of the Ministry of Supply, making use of a digital computer to perform the calculation and automatically to punch the tape.

(4.2.3) Tape Winders.

For each tape reader two winders are provided for coiling up the 150 ft length of tape and maintaining a tension of about 40 g(wt) on it as it passes through the reader. The winder comprises a shaft carrying the tape spool, and an aluminium disc driven as a Ferraris motor. The jerky motion of the tape occasioned by the reader is cushioned against the inertia of the tape spools by a lightly-sprung jockey pulley, and excessive run-in speeds are prevented by a permanent-magnet drag on the aluminium disc. Unwinding of the tape when the driving magnets are de-energized is prevented by a spring-actuated electromagnetically-withheld friction brake.

(4.2.4) Tape Material.

In carrying out a large number of trials to prove the performance of the modified tape reader, opportunity was taken to assess the relative performance of a number of materials for use as tapes from the point of view of repeated use, and also for punchability. Different grades of paper, impregnated fabrics and plastic sheet were tried. Parchment paper, 0·005 in thick, already in service as long-life telegraph tape, was considered most suitable; it is readily available and capable of withstanding some 2000 passes through the readers. One material was discovered with substantially better performance, i.e. Terylene sheet—but it was not then readily available.

(4.3) Jack Control Unit

The jack control unit consists of two parts—the pilot lead-screw, which sets up the required position for the jacking station and actuates the hydraulic valve, and the impulse receiving unit, which responds to the perforations on the control tape and rotates the pilot nut. For the purposes of clarity these will be dealt with separately.

(4.3.1) Impulse Receiving Unit.

In the early conception of the scheme it was proposed to drive the pilot nut stepwise, by the use of solenoid-operated ratchets, and an experiment was carried out employing the motor of a 'both-ways selector'. This was found to be unsuitable for driving a load with appreciable inertia, because this type of ratchet is not self-locking and there is a tendency for overshooting to occur. An arrangement involving two self-locking ratchets (uniselector motors) driving the nut through a differential gear was then tried, with more satisfactory results. However, the margin of torque available was not adequate to establish confidence, in face of the various possibilities of increased friction which might occur during protracted service. The development of a larger ratchet motor appeared likely to involve a number of unknown factors, and instead, attention was given to a mechanical drive, intermittently clutched. In the apparatus as developed, shown in outline in Fig. 5, an input shaft, continuously rotated by a 4-pole 50 c/s induction motor, is geared to two single-revolution clutches of the type employed in start-stop telegraph apparatus. The driven members drive the output shaft through worm reduction gears and are compounded by a differential. Thus an impulse of 50 millisec duration applied to either clutch magnet results in a rotation of the output shaft of 3·6° in the appropriate direction. The clutches and gearing are housed in an oil-tight aluminium casing, and are lubricated by a small pump on the motor shaft. The output shaft is fitted with a castellated coupling by which it may be presented to the pilot-nut gear-box to give the correct datum setting. To facilitate the setting-up process, it has been arranged that the unit can be stepped by hand; for this purpose either clutch can be released mechanically by one of two push rods passing through the aluminium casing, the motor being hand-wound to complete the action.

(4.3.2) The Pilot Lead-Screw and Gearbox.

At each working-section jacking station the two screw jacks are driven by a hydraulic motor from a common constant-pressure oil feed, and their movement is controlled by a hydraulic-sensitive valve. Full speed is obtained by displacing the piston of the sensitive valve by 0·0005 in in either direction.

Referring to Fig. 3, the precision lead-screw is mounted alongside each screw-jack pair, and being strain-free it acts as a master position-fixing element to which the screw jacks become

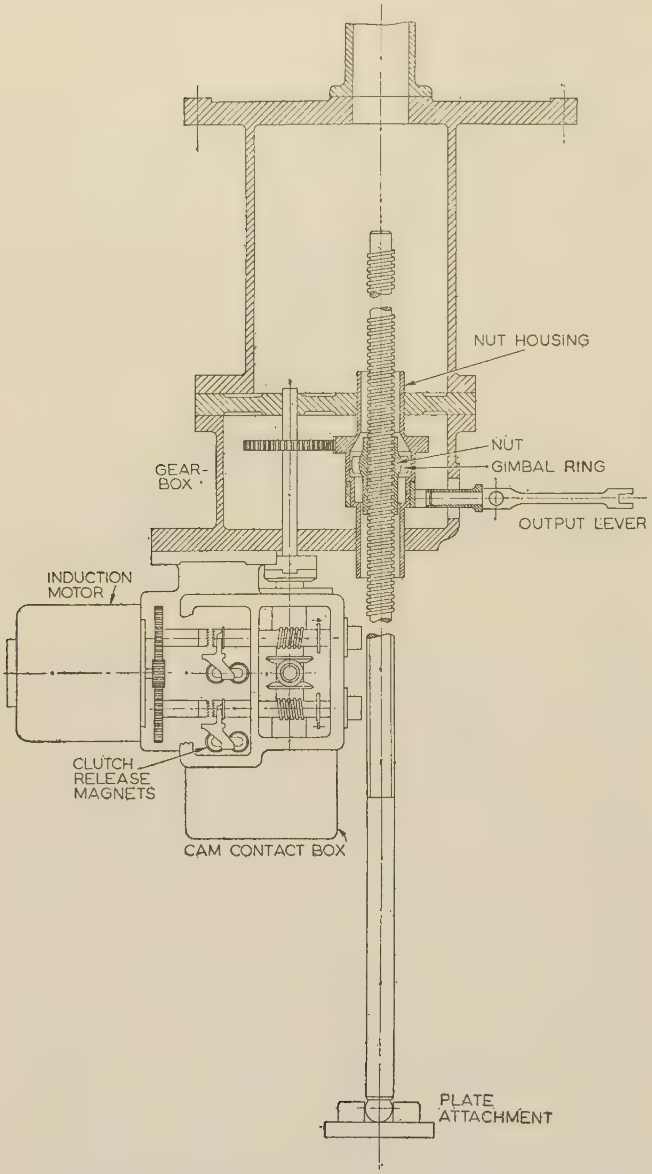


Fig. 5.—Impulse receiving unit and pilot lead-screw.

slaves. The foot of the lead-screw is attached by a ball-and-socket joint to one end of the transverse stiffener behind the plate, in line with the feet of the screw jacks; and the gear-box is fixed to an extension of the substantial beam-supporting screw jacks, so that the nut is on the same line as the trunnion axis on which the jacking system is supported. Thus the obliquity of the pilot screw very nearly follows that taken up by the screw jacks, but there is a slight discrepancy resulting from the fact that the foot axis of the lead-screw is 6 in farther from the surface of the plate than that of the screw jack. This needs to be taken into account in preparing the control data.

As the pilot nut is rotated by the impulse receiving unit and advances along the lead-screw, it moves with respect to the gear-box housing and displaces the piston of the hydraulic-sensitive valve by the same amount, via a 1 : 1 lever (shown more clearly in Fig. 5).

For jacking stations 2–22, which are at the upstream end around the supersonic throat, the pitch of the pilot screws is 4 threads per inch and the size of the steps is 0·0025 in; for jacking stations 23–31, where the total movement is less and the desirable degree of precision is greater, 8 threads per inch lead-screws are fitted, giving steps of 0·001 25 in. The accuracy of settling of the screw jacks with respect to the lead-screws is better than ±0·001 in, and the speed of following is such that when impulses are supplied at the maximum rate of 6·5 per second, the screw jacks are never more than two steps behind.

The pilot lead-screws range in length between 92·5 and 59·5 in overall, with threaded portions from 38·5 to 7·0 in, and have an accuracy of thread-cutting of ±0·001 in per foot (cumulative). The bronze nuts have been individually fitted, and the amount of backlash found between them and the screws at any part of the thread does not exceed 0·000 5 in. The nut, driven by a spur wheel, is borne in a cage supported in the forked end of the output lever. An extension of the pivot shaft of this lever enters and actuates the hydraulic-sensitive valve, and the lever itself operates a dial micrometer, which, at any instant, shows the error in following between the screw jacks and the pilot lead-screw. It also actuates one of two limit switches should the error exceed a set amount.

(4.4) Monitoring System

(4.4.1) Duplication for Safety.

In order that any error in the operation of a jacking station may not pass undetected, duplication of essential apparatus provides a means for checking the correct performance of each step. For each station, a second tape reader is fed with identically punched tape and it operates two monitoring units (one for each wall) which are located in the equipment room. The monitor has two main functions—it displays the net number of impulses received on a 5-digit counter, thus showing the position the pilo nut should have reached, and secondly it checks that the impulse receiving unit in the tunnel has responded correctly. This check is carried out continuously by cam-operated contacts fitted to both units co-operating in a circuit, a simplified version of which is shown in Fig. 6. These cam sets are rotated through 1/20 revo-

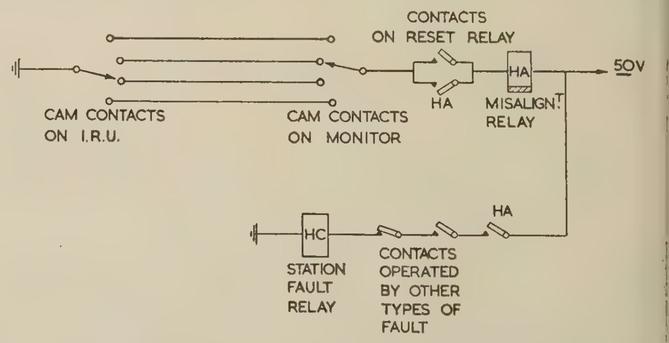


Fig. 6.—Simplified circuit for misalignment detection.

lution for each step taken by their respective actuators, and each have four 5-lobe cams with dwells covering single step positions and five single-lobe cams with dwells covering four adjacent steps. Each cam operates two normally open pairs of contacts which are, in fact, relay spring sets. It will be apparent from Fig. 6 that, provided that the two units remain in step, there is always a path to keep the misalignment relay energized. However, if the units fall out of step in either direction this path is broken, the relay releases and prevents further automatic operation until the defaulting unit has been corrected. Actually the impulse receiving unit and the monitor do not move exactly together, because of the different designs of their actuating mechanisms; the misalignment relay is therefore slugged to delay its release by 150 millisec, in order to cover the momentary breaks in current as each step is made.

(4.4.2) **The Monitor.**

Development of the monitor arose out of the need for a counting device which could be stepped forwards or backwards by the application of electrical impulses—no suitable article appeared to be available at the time. Fig. 7 is an explanatory diagram of

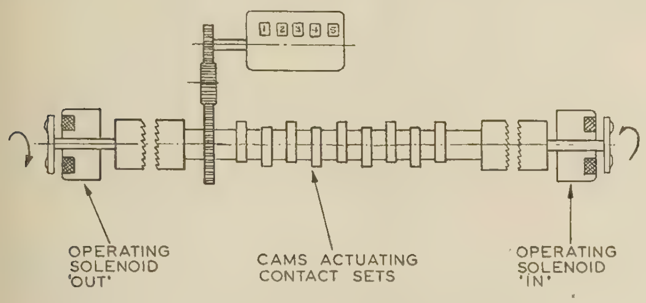


Fig. 7.—Diagrammatic arrangement of monitor.

the general arrangement. The actuating magnets (one for each direction) are a proprietary pattern in which the linear pull on the armature is converted into a rotary movement by the action of balls running on a ramp. This mixed linear and rotary motion of the armature, resulting from the application of an electrical impulse, is used to engage a ratchet-toothed clutch and turn the main shaft through 18°. The shaft is then held in the new position by a roller detenting in a 20-toothed star wheel, whilst the armature retracts at the end of the impulse under the influence of a spiral spring. The main shaft carries the nine cams which perform the monitoring function, and through a 2 : 1 spur gear drives a 5-digit cyclometer-type counter.

(4.5) Automatic Setting of Flexible Walls

(4.5.1) **Mach Number Selection.**

When it is desired to alter the air speed in the supersonic range, the operator selects the new Mach number on a 3-decade switch on the control desk and sets the automatic system in operation by pressing a start button. The Mach number reached by the system at any instant is recorded by a specially punched tape fed through a tape reader in step with the control tapes. This reader operates three Mach number counters, one for units, one for tenths and one for hundredths, and the Mach number is displayed as a 3-digit number on the control desk. The Mach number counters are basically similar to the monitors already described in Section 4.4.2. Fig. 8 illustrates the punching code used for these special tapes. When the tape is being indexed forward (Mach number increasing), the presence of a perforation

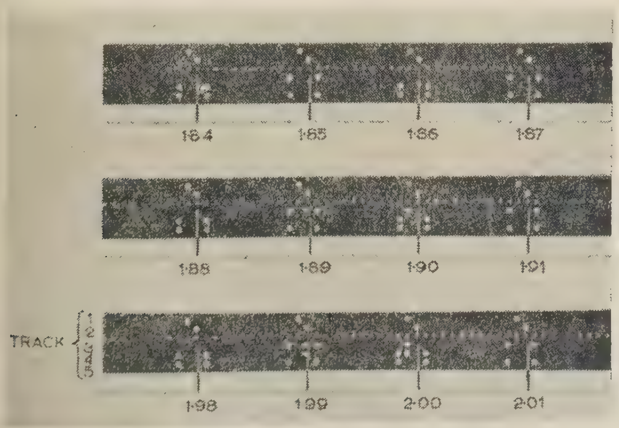


Fig. 8.—Tapes for Mach number control.

in track 1 causes the counter for hundredths to step up one; the presence of perforation in tracks 1 and 3 together causes the counter for tenths to step up one as well; and similarly perforations in tracks 1, 3 and 4 cause all three counters to operate, e.g. for a change from 1·99 to 2·00. When the tape is travelling in the reverse direction, reading is initiated by perforations in track 2 instead of track 1, and a similar action ensues. However, should the action be stopped by depression of the operator's stop button between denoted Mach numbers, and then restarted in the reverse direction, the last number read from the tape must not be read again. This is circumvented by the punching of perforations in tracks 4 and 5 immediately before and after every even number, and in tracks 3 and 5 before and after every odd number. Cam-operated contacts fitted to the hundredths counter detect whether the last number read was odd or even, and it is arranged that the counters ignore a set of perforations which may specify a number of the same sort. To prevent errors arising in the reading of the Mach number tapes, two tapes are provided and the units are duplicated; the three counter units are separately checked by their counterparts in a manner similar to that employed for jacking stations.

(4.5.2) **Initiation of Automatic Action.**

The three selector switches, in conjunction with the position of the cam-operated contacts of the counter units, determine whether the selected Mach number is greater or less than that recorded from the tapes, and if it is less the reversing solenoids of all the tape readers and the impulse interchange relays become energized. A simplified circuit diagram is given in Fig. 9, where

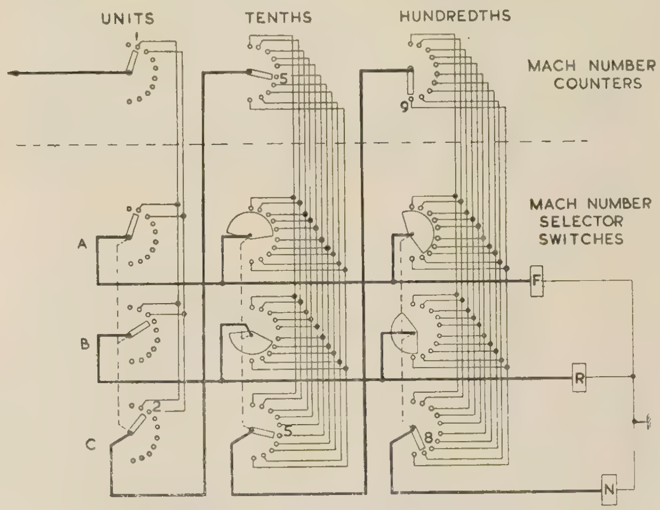


Fig. 9.—Simplified circuit—Mach number selection.

the three counters are shown reading 1·59 whilst the selector switch is set for 2·58; i.e. for the units the required value is greater, for hundredths less, and for the tenths equal. It will be observed that the interconnection between the switch banks and the cam contacts of the counters joins the common connections of the latter to the A wafer of the corresponding switch if the required digit is the greater, to the B wafer if it is less, and to the C wafer when coincident. If there is not coincidence in the most significant digit, relay F will be energized from the A wafer, or relay R from the B wafer. However, when there is coincidence, a circuit is made via the C wafer to the next digit, etc. Thus relay F is operated whenever the required Mach number is greater than the existing, and it prepares the control system for operation to increase the Mach number on subsequent operation of the start button. When the tapes have advanced far enough

to give parity for all three digits, relay F becomes de-energized and relay N is operated to show that the required Mach number has been reached. However, if the stop button is operated before this, relay F becomes released but relay N will not be operated. Similarly operation of relay R would prepare the system for reduction of Mach number. Should the operator, having started the system, change his plans and select a different Mach number, the system will proceed to the new Mach number provided that it does not involve reversal of the tape readers. If it does, it is momentarily equivalent to the selected Mach number having been reached, and the system will automatically stop; a second depression of the start button must then be made. In addition to the stop button which halts the action of the control system alone, emergency stop buttons are provided on the control desk, in the equipment room and in the tunnel (for use during testing), which not only stop the electrical control system but also release hydraulic pressure from the jack motors.

(4.6) Manual Adjustment System

(4.6.1) Manually Inserted Impulses.

In order to provide the required facility for varying slightly the profile of the flexible plates by hand, a series of spring-centred telephone keys is situated at the right-hand end of the control desk. These only become operative when an auto/manual change-over switch has been set in the appropriate position. Then, whilst a key is operated, a train of impulses may be injected into the corresponding jacking station, its monitor, and its 'manually added ordinate indicator' displayed above the key, the effective direction of the injected impulses being determined by whether the key is moved up or down. For convenience, the impulse rate is reduced to 3·25 pulses/sec, and the impulses originate from the pulsator which is set in motion by the operation of the auto/manual switch. The circuit arrangement shown in Fig. 10 has been adopted in order to ensure that impulses are not clipped either at operation or release of the keys. Setting the auto/manual switch to the manual position energizes a number of contacts on the pulsator, QC1, QC2, QC3 and QC7 (referring to a given station), which give impulses of 50 millisec duration. On depressing the key, to produce an outward movement of the jacking station, relay YB is energized, but only when contact QC7 is in the back position, and this relay then holds over its contacts YB6 so long as the key is kept closed. When the key is re-opened the relay remains energized so long as contact QC7 is in the process of delivering an impulse over its front contacts. During the period the key has been closed, a succession of impulses will have been delivered via contacts QC2, QC3 and QC1, and relay contacts YB1, YB2 and YB7, to the 'out' magnet of the monitor, the impulse receiving unit and to the pecker withdrawal magnet of the manually-added ordinate indicator (see Section 4.6.2). This causes the display band and control tape to move in the required direction, by virtue of contact MC1 and the relay contacts YB3. Should the ordinate indicator reach its limit of 50 steps (or 100, in the case of the stations with 8 threads per inch lead-screws) the limit relay YM prevents further impulses being supplied in that direction by the de-energizing relay YB. Relay YB, and the corresponding relay YA for inward motion, are electrically interlocked to prevent both being operated at the same time should the manual insertion key suddenly be thrown across.

(4.6.2) Manually Added Ordinate Indicator.

The unusual design of the manually-added ordinate indicator, shown in Fig. 11, arose from the expressed desire of the users of the tunnel to have a display in the form of a band, half yellow and half black, moving vertically against a scale marked in steps. Moving in step with this band, there is a perforated tape from which a set of peckers obtain revertive signals for checking the correct operation of the indicator, and also for operating the limit relays YL and YM mentioned above. The ordinate indicators are of two patterns according to whether they are used for stations with pilot screws having 8 or 4 threads per inch. For the former the display band moves 0·05 in per step and for

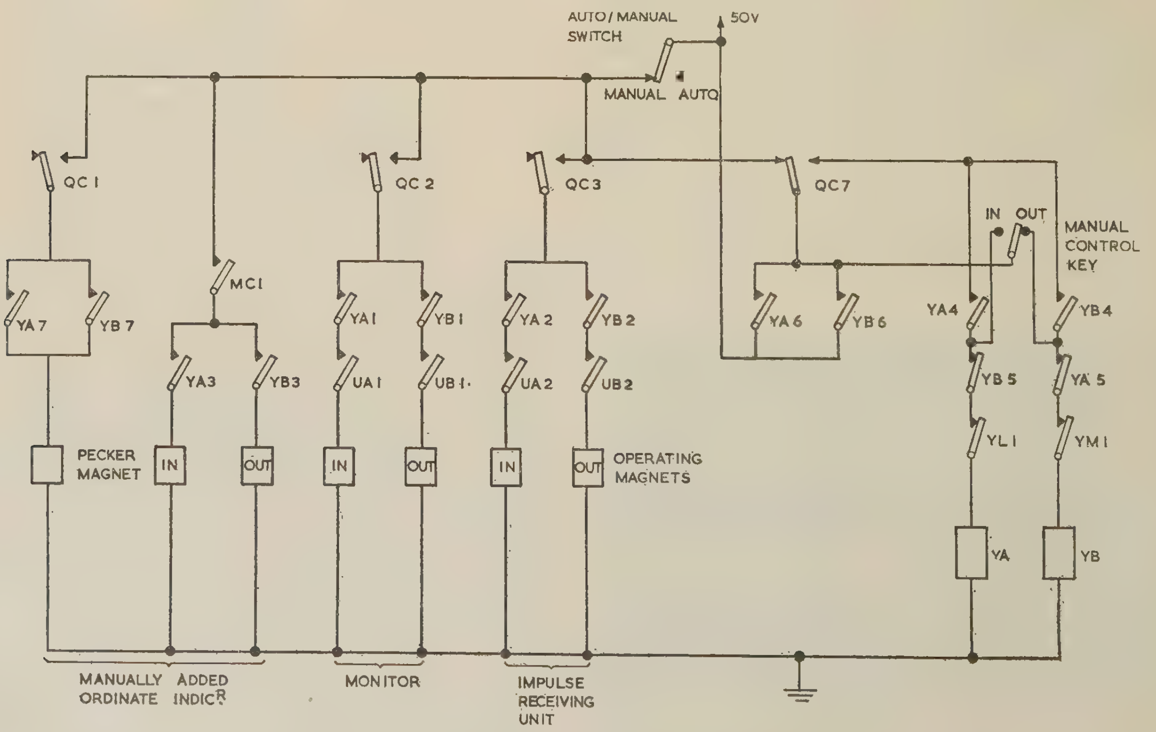
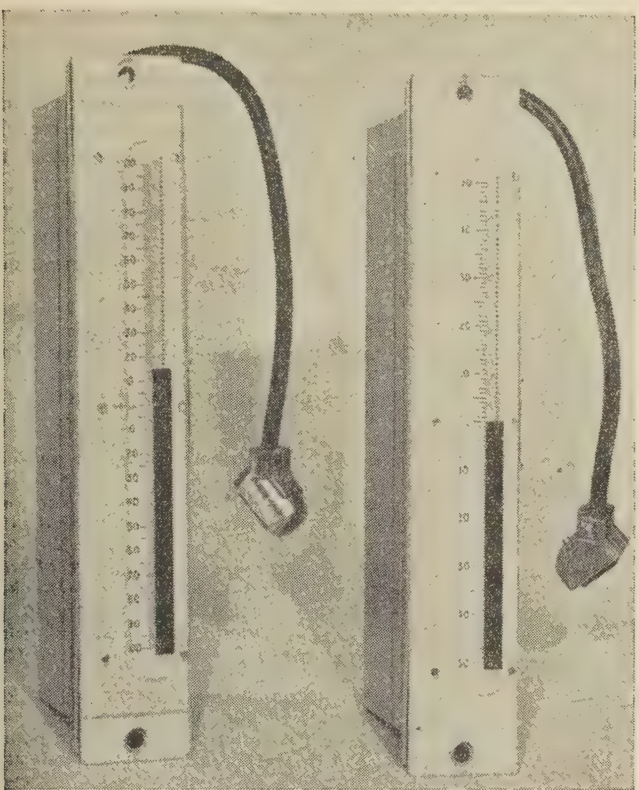


Fig. 10.—Simplified circuit—manual impulse injection.



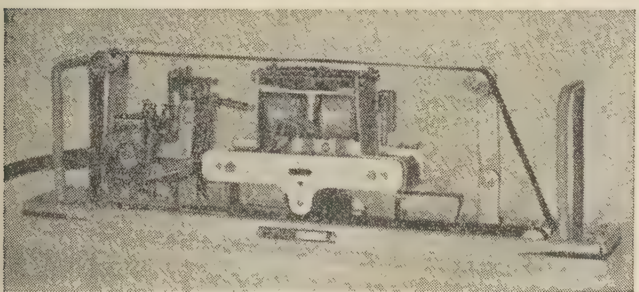


Fig. 11.—Manually added ordinate indicator.

the latter 0·1 in per step, so that in either case the displayed movements are proportional to actual jack movements. In both patterns the monitoring tapes move 0·1 in per step, i.e. by one row of perforations. The motor of a both-ways selector is used to index both the display band and the monitoring tape, by pin wheels of differing sizes. The revertive signals are obtained from four relay spring sets, separately actuated by cranked levers, which at their other end seek the perforations. A solenoid can raise all the levers clear of the tape, prior to operation of the tape-shifting magnets.

(4.6.3) Checking of Ordinate Indication.

Testing for the correctness of the operation of each manually-added ordinate indicator is of importance on two counts:

(i) If manual adjustment has been made to a station, and subsequently is not completely removed, although the ordinate indicator purported to show that it had been (and if this were to happen repeatedly), it would result in the corresponding jack wandering from its tabulated extension.

(ii) Use will be made of the manual facility to reduce, by experiment, minor irregularities in air flow in the neighbourhood of the model. Correct display of the amount of manual insertion will therefore be valuable, and from it more refined data for automatic operation can be developed.

The operation of the ordinate indicators is therefore checked with the monitors step by step, and the manner by which this is accomplished differs from that previously described, because manual operation can be brought into action with quite arbitrary relative positions of the two units. The circuit arrangement is shown in Fig. 12. Revertive impulses from two of the pecker contacts operate the relays YN and YO alternately as the indicator moves. These relays co-operate with two cam-operated contacts K1 and K2 of the monitor, which likewise close alternately to actuate the four double-coil relays YP, YQ, YR and YS. When the auto/manual switch is moved to the manual position, two of these four relays will be energized and will remain so, through their second coils and their own contacts. Thus, if both units are resting on odd, or both on even, positions YP and YS will operate, or if one is at an odd and the other at an even position YQ and YR will operate. During the subsequent manually-injected impulses, if both units step together this condition will persist, but if either unit fails to make a step a third one of the four relays will become energized. This results in a break in the holding circuit for the detection circuit YD, which signals the fault to the central fault-location array. A contact YD4 meanwhile by-passes the auto/manual switch until the fault has been rectified, as otherwise the relays could be reset by moving the switch.

(4.7) Fault Location and Correction

The 60 fault-detection relays mentioned in Section 4.4.1 must all be in the energized condition for the control system to continue functioning. If a misalignment occurs on any station, the corresponding relay is released and so prevents the pulsator from delivering further impulses to all circuits; also the power supplies to the motors driving the tape readers and the impulse receiving units are interrupted. The erring unit has to be found and rectified before the system can be restarted, and a centralized testing equipment for facilitating this is provided in the equipment room. The release of one of the relays lights a fault lamp associated with the station, and also energizes a contact on the bank of a uniselector. Upon depression of a switch, the wipers of the uniselector seek and find the faulting station (or one of them if there are more than one), and connect the circuits of the detection apparatus into the station circuits. The step positions of the impulse receiving unit and its monitor are then indicated on two rows of 20 lamps, making use of the second set of contacts operated by the cam sets on either unit. As shown in Fig. 13 these contacts are connected to give the effect of a single-pole 4-way and single-pole 5-way switch, the ways being connected by a multiple of nine wires, there being one multiple around the monitors and another around the impulse receiving units. Selection of the station by the uniselectors energizes the common cam contact on the particular station and hence two wires of each multiple, according to the existing state of the cams. Thus, via relays, the position of the cams of either unit is displayed within the range of 20 steps. Having discovered a misalignment, its location needs to be determined—one unit may have failed to make a step, the other unit may have taken a step when it should not. The lamps indicate the relative position of the impulse receiving unit to its monitor, but alone do not show which is incorrect. However, the position of the monitor may be checked from the reading of the 5-digit counter, which may be compared against a tabulated list. To enable this to be done, a 'step counter' is provided to show which row of the tapes is being read. This instrument is of the same design as the monitor unit, and is driven directly from the pulsator in step with the tape readers. It is now possible to correct the unit which is out of step by applying one or more impulses to operate it in the required direction, and when this has been done the automatic

operation of the system can be restarted, provided that the other stations are also free from faults. The circumstances of the occurrence of a fault may give useful evidence as to its location. Thus a tape reader, failing to index its tape, results in faults on upper and lower jacking stations simultaneously (or sequentially if the tape punching differs). On the other hand, if the motor driving a pair of tape readers should stop, faults will occur on two adjacent pairs of stations.

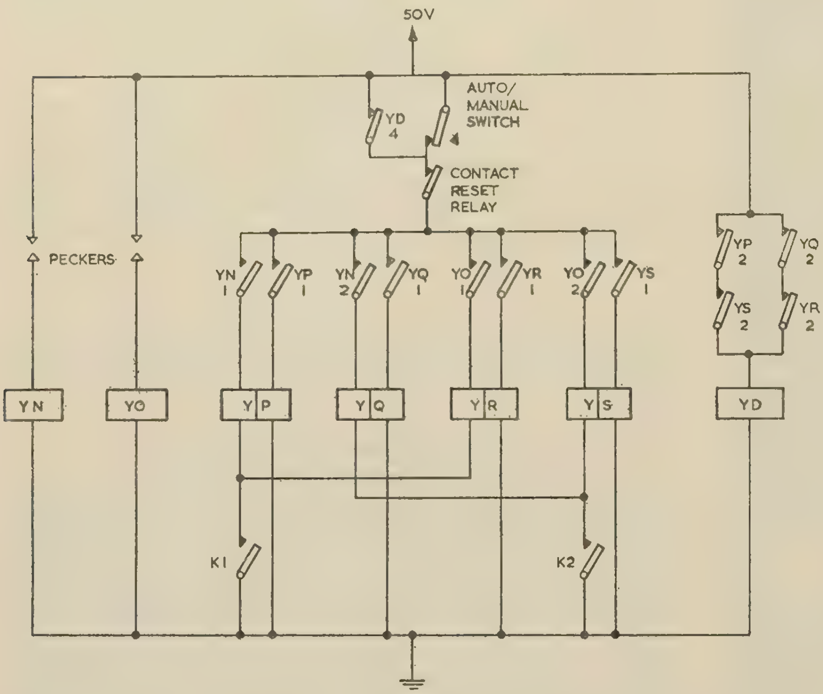


Fig. 12.—Simplified circuit—checking of ordinate indication.

(4.7.1) **Major and Minor Faults.**

The type of fault discussed above is considered as a 'minor fault', since it can be rectified and operation of the control system resumed without the need for opening and entering the tunnel pressure shell. Occurrences of a nature which cannot be rectified in this way, and in particular those which might endanger the safety of the flexible plate through over-stressing, are treated as 'major faults', and safety circuits have been provided so that the hydraulic-pump motors are tripped as well as stopping the further action of the tape impulsing. Major faults include the following:

(*a*) A hydraulic servo mechanism failing to respond to its mechanical input, or 'running away'. This is detected by limit switches operated by the output lever of the impulse receiving unit, set to operate when a discrepancy corresponding to ten steps has been reached.

(*b*) The unlikely, but possible, 'running away' of an impulse receiving unit owing to its clutch remaining in engagement. This is detected by the continuance of signal impulses from the driven shaft after the corresponding fault-detection relay had been released. For this purpose, two special pairs of break contacts are fitted to the cam set, and the interlocking circuit is arranged so that power supply to the impulse receiving unit motors is not cut off before they will have made at least four complete revolutions.

(*c*) Excessive strain in the plate at any jacking station. Curvature of the plate adjacent to every jacking station is measured by an inductive-pattern transducer, and displayed on individual indicating instruments on the main control desk. Excessive displacement of any transducer actuates a relay which initiates a 'major fault' warning. Because of the crucial importance of this apparatus as the ultimate measure of the safety of the flexible plates, auxiliary equipment is fitted, i.e. a routiner, which tests the correct operation of each curvature gauge and interlock circuit, in turn continuously, whilst the tunnel is being used.

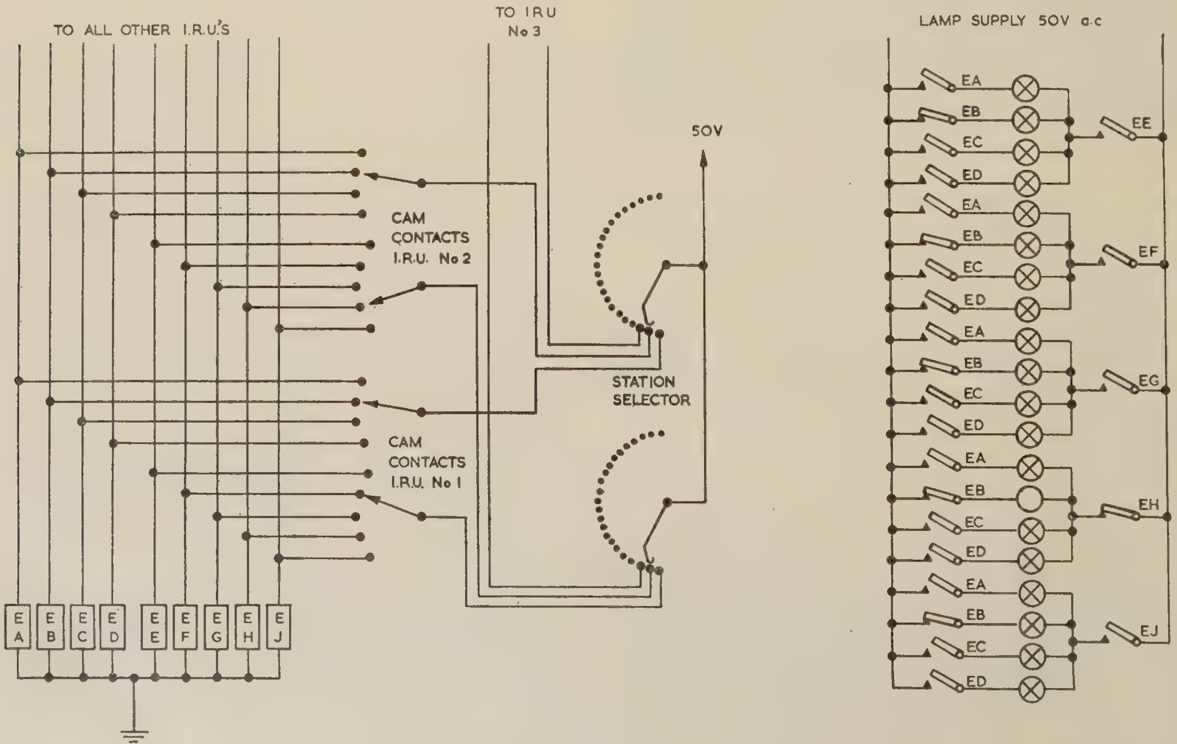


Fig. 13.—Simplified circuit—position indication.

(4.8) The Pulsator

The primary impulse machine, or pulsator, has two main functions: (i) in automatic operation to provide the start impulses to each tape reader, and (ii) in manual operation to provide a series of impulses as required to operate each jacking station. The pulsator comprises three parts indicated schematically in Fig. 14. A rotating part driven by a 3-phase induction motor carries two electromagnetically released dog clutches, one controlling a shaft operating 64 sets of cam-operated contacts, PC, for automatic operation, and the other a shaft with 60 sets of contacts, QC, for manual injection. The former rotates (when its clutch is engaged) at 390 r.p.m., so that the PC contacts give impulses of 50 millisec duration at a rate of 6·5 per second. This is slightly slower than the speed of rotation of the tape readers, so that the latter are able to complete their action before receipt of the next start impulse. The manual control shaft rotates at half speed to enable the number of steps manually injected to be the more nicely judged. There is an additional facility for running the system automatically at half speed, which may be useful in order to keep the tunnel functional in an emergency caused by partial failure of the hydraulic supply. This is provided by the repeated actuation of the PC clutch on a start-stop basis from a contact operated by a cam on the constantly rotating shaft. Thus impulses are provided to the tape readers at the reduced rate without their duration being lengthened.

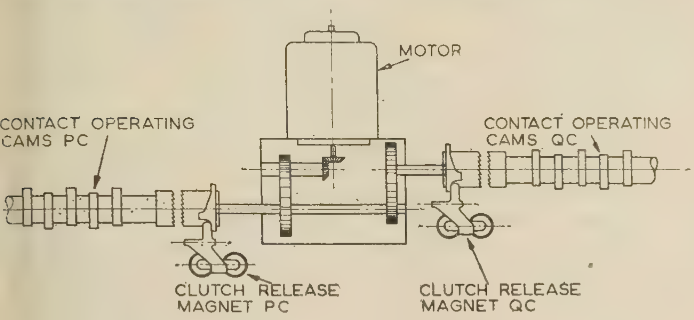


Fig. 14.—Diagrammatic arrangement of pulsator.

(5) THE SUPERSONIC DIFFUSER

The supersonic-diffuser throat, which lies immediately downstream from the model, has fixed floor and roof. Between these the side walls (each consisting of five flat panels hinged together) slide, under the influence of three 150-ton screw jacks, near the intermediate hinge points. There is a fourth jacking station on either side which acts substantially between the second and third moving panels to prise openings through which air may re-enter the circuit after by-passing the working section (it has left the main circuit by a number of orifices around the contraction fairing). This facility is required when the tunnel is operated at high air speeds because there would not otherwise be a sufficient mass of air flowing to permit the main compressors to continue working without risk of surging. Automatic adjustment of the supersonic diffuser in correspondence with that of the working-section throat is arranged by punched-tape control, making use of the spare tracks on eight of the existing tapes. Owing to the considerably lower degree of positional accuracy demanded, it has been possible to use a simpler control scheme than that for the working section, and this is shown in block schematic form in Fig. 15. For each station the required position is set up by rotation of a Magslip coincidence transmitter in the equipment room. This is geared to the shaft of a modified monitor ('main control unit'), operated from the tape reader, so that each impulse received rotates the Magslip rotor by 0·18°, corresponding to a demanded jack motion of 0·005 in, and the demanded motion is displayed on a 4-digit counter to the nearest 0·01 in. A co-operating Magslip transmitter is geared to the hydraulic motor driving the jack, and associated with it is another counter for use during setting-up. The output signal is fed to a magnetic amplifier which energizes one of two signal relays whenever the relative Magslip misalignment exceeds 1·6° and an alarm relay should the misalignment reach 15° in either sense. The signal relays operate repeater relays, which, in turn, energize solenoid-operated valves (under the overriding control of limit switches in the tunnel) to admit oil to the jack motor.

Because identically punched tapes are employed for the automatic control of corresponding starboard and port stations, the two main control units are made to monitor each other, and fault indication and correction circuits are arranged as for the working

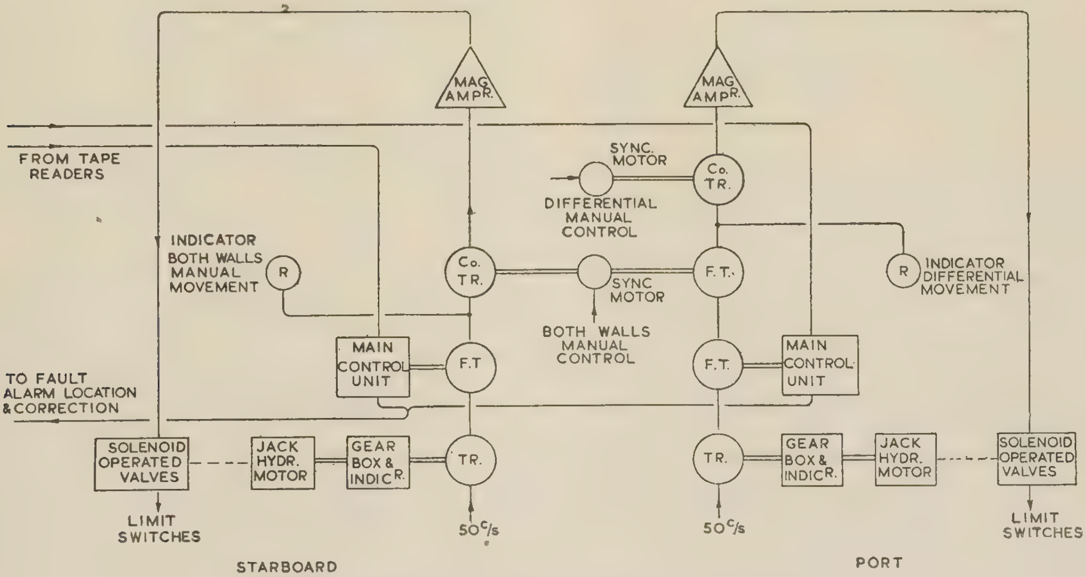


Fig. 15.—Schematic of control system for supersonic diffuser.

section. Manual control of the diffuser may be required either to adjust the constriction to improve pressure recovery, in which case both walls will be moved equally, or to counteract skewing of the air stream by reaction from the model when it will be necessary to provide a differential movement. For the first, two follow-through Magslip transmitters are inserted between the transmitter and coincidence transmitter, one for the starboard station and the other for the port. For the second, another follow-through transmitter is fitted for the port side only. In either case, manual control is effected by rotating appropriate Magslips by miniature 2-speed reversible motors operated from the control desk. Manual control is available at any time, and in order that the combination of demanded speed from the two manual controls and automatic operation shall not exceed the available speed of the jack motor, an interconnection of relays is arranged automatically to reduce the manual-control motors to half speed, or to stop them, when necessary. The speeds concerned are as follows:

Maximum available rate of jacks: 2·0 in/min.
Maximum automatic demand rate: 0·9 in/min.
Manual control rate, full speed: 1·8 in/min.
Manual control rate, half speed: 0·9 in/min.

The circuits for controlling the by-pass door jacks do not materially differ from those for the wall-panel jacks, but an over-riding circuit is provided whereby depression of the anti-surge button will always set the normal solenoid valves to open the doors. At the same time an extra valve is opened, and further pumps are started, in order to hasten the action. For testing or setting-up purposes individual jacking stations may be operated from inside the tunnel, by the use of a portable plug-in push-button controller. Indicators, in the form of Magslip receivers, are provided on the control desk to show the amounts of movement manually introduced.

(6) LAYOUT, INSTALLATION AND TESTING

Of the considerable quantity of equipment involved, only the essential controls and indications for operating the system are mounted on the main control desk. The main part (excepting, of course, the pilot screw units) is housed in an adjacent equipment room and is arranged on racks of the telephone repeater pattern. In general, there is one bay to hold the equipment for each pair of jacking stations, but care has been taken in arranging the circuits that no foreseeable failure of a piece of apparatus will affect both a controlled unit and its monitor. To facilitate the maintenance of the rack-mounted equipment, a portable test unit has been provided, which may be connected to any rack by flexible connectors. The unit contains the essential components of a jacking station—principally, an impulse receiving unit and a manually added ordinate indicator, and means for operating and thereby testing all the normal functions of each rack in turn.

(7) OPERATING EXPERIENCE

Operation of the tunnel for calibrating purposes, model testing and the commissioning of auxiliary equipment has been taking place during the past six months. Consequently, sufficient experience has been gained for some conclusions to be drawn as to the suitability and effectiveness of the nozzle control system. These relate to two main features:

First, mention may be made of the difficulty in calculating aerodynamic nozzle shapes, which, at the same time, result in the minimum stressing of the flexible plates. In fact, the initial set of punched tapes produced nozzle shapes involving relatively high stresses. A combination of further calculation with the use of the manual adjustment system (Section 4.6) enabled the information for a final set of tapes to be made available in a very short time.

Secondly, the checking circuits and other safety features incorporated in the system have shown good indications of reliability. It was a matter of some concern whether, with the multiplicity of individual circuits required for the simultaneous monitoring of all the equipment, there would be an excessive rate of fault occurrence. Such a situation would, of course, result in an undue waste of tunnel operating time, besides being a source of annoyance to the operating staff. However, the number of transient faults has been remarkably few, and in no instance has a fault of any kind occurred without its nature being revealed on the fault-indication panel.

The most frequently occurring cause of 'minor faults' has been the failure of one of the tape readers to index forward its tape; in general, this has been rectified by readjustment of the mechanism. As experience in making the adjustment is gained, this particular difficulty should be mastered, and the whole equipment should then be largely trouble-free.

(8) ACKNOWLEDGMENTS

In presenting the paper, the authors wish to pay tribute to many colleagues in the organizations which they represent, in the Ministry of Supply and in other firms, who have collaborated in the development of the scheme and particularly in the design of the special equipment upon which the success of the scheme is so dependent. They are also indebted to the Director, G.E.C. Research Laboratories, the Chief Engineer, Ministry of Works, and the Director, Royal Aircraft Establishment, for permission to publish the paper.

DISCUSSION ON THE ABOVE FOUR PAPERS BEFORE THE INSTITUTION, 9TH JANUARY, 1958

Mr. P. T. Fletcher: During the war, wind-tunnel facilities in this country were limited to about 4 000 h.p., mainly for subsonic flow. In 1944 the Government proposed to expand our aeronautical research facilities by including a new national aeronautical research establishment. A search was made for a suitable site based on the availability of power. The Highlands were considered, with pumped storage, but after examining all the conditions, such as access and load factor, a site was chosen north of Bedford where 200 000 h.p. could be supplied from the Grid.

In 1945, a British technical mission was allowed to examine aeronautical research facilities in the United States. These had been expanded rapidly and included a whole range of subsonic, high-subsonic and supersonic wind tunnels, an engine-testing wind tunnel and altitude test beds for piston engines. Large American subsonic wind tunnels were in the range of 16 000–40 000 h.p. and the most common choice of plant was modified Kramer, with speeds down to 320 r.p.m. at 40 000 shaft-horsepower, with about 3 000 h.p. per motor pole.

For high-subsonic and near-transonic tunnels, induction motors were being used for the main power component of the drive, coupled to d.c. motors with Ward Leonard control. A total power of 12 000 h.p. at 570 r.p.m. with 3:1 a.c./d.c. sharing was typical. For supersonic tunnels and large engine-testing installations, induction motors were up to 30 000 shaft-horsepower per motor and speed control by rotor regulation was used. Liquid regulators were temperature controlled, with attention to mechanical stability.

Commercially there was little difference in cost between the Kramer and mixed a.c./d.c. drives. A serious attempt was being

made to lower costs by adopting synchronous motors driving through eddy-current couplings. Two examples were of 12000 and 7000 h.p. each at 740 r.p.m. The control characteristics appeared to be very good, since both electrical and mechanical inertias were low. Prices appeared to be about half those of other drives.

The large R.A.E. wind tunnel uses a variation of the composite a.c./d.c. drive. I should like to know whether the authors now regard a d.c. component regulated in speed by a voltage-comparison method as the optimum choice.

I should like to compliment the authors on the ingenuity both in conception and in execution of the variable-nozzle system. The alternatives of a removable nozzle block, a sliding nozzle block or flexible walls pressed against templates, all of which have been tried in other installations, are far from elegant. However, they had to introduce electrical complications, and I should like to know whether the short time required to set up the nozzles initially by repunching tapes has justified the apparent complication.

At the end of the war this country took possession of a number of German aeronautical research facilities and so obtained some electrical power plant of obvious capital value and potential usefulness. While the survey which the authors have given quotes examples of the work of almost every major manufacturer of electrical plant in this country, it also includes some examples of ex-German machines which were virtually new when taken over. Among these are the 6000 h.p. d.c. motors of 600 r.p.m., the 3 ft × 3 ft wind tunnel and d.c. machines of the Ward Leonard loops of the 8 ft tunnel at the R.A.E. Bedford. These machines, which came from the Hermann Goering Aeronautical Research Establishment near Brunswick, were installed in the United Kingdom by British firms.

Mr. A. Asbury: With reference to Paper No. 2501 U, it is difficult to get a conception of the accuracy of 0·02% achieved. If a watch had this accuracy, however, it would only have an error of 17 sec a day.

Another point of interest in the speed control is that the 'tail' is arranged to wag the 'dog'. A further reason for this is that the system chosen allows use of the gas-turbine output in a manner which does not conflict at steady state with the control by the d.c. machines.

The top step of the pattern of Fig. 10 gives the maximum rate of charge of fuel allowable, and the component which supplies the bulk of this has a large dead band around zero error which prevents it from affecting the cross-over point. The small step provides a fuel-increase rate which is small enough to prevent unwanted torque swings when near zero error. Since this component is directly derived from the direct machine current it cannot interfere with the speed required by the control system.

Eqn. (12) uses the fact that the torque constant is equal to $0{\cdot}74\,k_1$ lb-ft per ampere. The figure of 0·74 arises from our peculiar unit system and is, in fact, the ratio 550/746. One advantage of using the M.K.S. system is that this constant becomes unity.

In large equipment of this kind, maximum utilization is only secured when the data obtained from experiments are in a form which enables them to be fed directly into a digital computer. Results can then be obtained while the model is still in the tunnel to check that adequate ranges of parameters have been employed.

Mr. J. F. M. Scholes: These remarks refer to Paper No. 2524 U. Whether we be concerned with design, manufacture, installation or maintenance, many of us when confronted with a finished equipment must often wonder whether it could not have been done better another way. The basic concept for this system was laid down some six years ago, and there has been time for considerable development in the data-handling field in the intervening period. It is interesting to examine the concept used in the light of our further knowledge and experience.

In this case a discrimination of one part in 16000 is necessary at each jacking station, and it is clear that a digital technique should be chosen. We have then to decide whether to use an incremental technique or a fully digital one. A fully digital system would require the storage of at least 14 million bits of information, while the present incremental system requires some 4 million bits, including duplication for error detection. It would seem hard to justify a departure from the incremental system.

Advantage could be gained, however, by making use of some new equipments that have become available. We should almost certainly still use punched or magnetic tape, but would probably make use of electronic reading methods. One of the shortcomings of the present system is the speed of operation, and this could be multiplied manyfold, although a factor of two or three would be entirely adequate.

Developments in multiple-track magnetic tape and linear-coded scales may favour a fully digital system at some future date. I favour the existing system at present, although fully digital methods are used for all data recording in this and other tunnels at the R.A.E. Bedford.

Mr. J. McTaggart: In Sections 9 and 10 of Paper No. 2501 U three methods are used to analyse the performance of the speed control system:

(i) By inverse Nyquist frequency-response diagrams.
(ii) By the solution of the operational equations.
(iii) By setting up on an analogue computer.

The first two methods are limited mainly to simple linear systems and are very tedious, several hours being required for the analysis of the system represented by Fig. 15. The same problem can be set up and solved on an analogue computer in 8 min. A range of additional conditions can be studied and recorded, as in Fig. 19, in a further 3 min. Therefore, in view of the speed of an analogue computer it is surprising to find that the other methods of analysis were used when a computer was apparently available.

The control system described uses transient feedback from the tachogenerator to stabilize the speed-control loop. The resulting response is still rather oscillatory and has a pronounced oscillation with a period of about 5 sec. Is this an acceptable response, or were additional feedback circuits included? The addition of transient feedback from motor current, for example, would practically eliminate the oscillation without increasing the response time.

I do not agree with the argument at the end of Section 9.2, for the choice of the gain m_1. First, regulation is not mentioned in the control requirements (Section 4) but rather the stability at virtually constant load, which is quite a different matter. Secondly, as the authors point out, the integral control eliminates the effects of regulation. I suggest that m_1 should be selected (i) to minimize the short-term disturbances caused by mains voltage and frequency variations and (ii) to stabilize the integral control loop, particularly where backlash may be present.

Mr. T. Roberts: With reference to Mr. Scholes's comments on incremental as against positional digital control, it is safe to say that incremental control is better in this case for two reasons. The wall itself moves from one position to another by going through a complete range of required positions. An indication is needed in the control room of the position of each jack, and use is made of this fact to incorporate a device to monitor the system. This is of the utmost importance, since there is a very high premium on reliability.

With regard to the number of bits of information, or, in other terms, the length or width of tape or other medium which would

be used, it is of interest to reflect that 16000 holes, which represents the total movement of one of the jacks, is approximately 2^{14}. A little thought will show that it would need a great deal of tape to programme the movement of this jack on a truly digital position basis.

Mr. J. M. Ferguson: With reference to Paper No. 2414 U, at the time the drive was designed it is true there was very little information on the breaking capacity of circuit-breakers for anything other than standard frequencies. Some data from the United States were available, which seem to have had very little published test information to support them. Since the paper was written, however, short-circuit tests have been made to check the rating of the circuit-breakers at 25 and 10c/s. These tests indicate that the American levels which were assumed are very conservative.

Satisfactory interruption under short-circuit conditions was obtained at currents equal to the rated rupturing capacity of the circuit-breaker at standard frequency, with the recovery voltage reduced in proportion to the frequency. These results have now been obtained on both oil and air circuit-breakers.

Mr. J. J. Thompson: I find it difficult to understand why such an expensive and complicated electrical drive system with variable-frequency supply from gas-turbine alternators should have been adopted for driving the wind tunnel. By far the most economical arrangement would have been direct drive of the compressor from a geared steam turbine supplied from low-water-content boilers of the Velox type. These would occupy little more space than the existing Ward Leonard set and could have been used for driving other tunnels. Quick starting is an inherent feature of boilers of this type, and the steam turbine could have been adapted to suit in this respect without undue difficulty. Ordinary double-helical parallel-shaft reduction gearing such as is used in marine practice would have served, although epicyclic gears would probably have been more economical in cost and space. An installation of well-proven engineering equipment would have been preferable to one using gas turbines, which, in the present state of the art, require a great deal of expensive development work. The steam-turbine system would have avoided the cost of gas turbines and the expensive building which houses them. I will not go into unnecessary detail but merely point out that the speed control of a steam turbine would have satisfied all requirements in spite of the load changes expected on site.

On a point of detail, the voltage-comparison method of speed control with its elaborate and delicate tachometer generator system is, in my opinion, hardly justified. The authors dismiss the use of a frequency-sensitive circuit by reason of ineffectiveness over the required speed range, in spite of the fact that single sweep control over the order of speed range required is fairly commonplace.

NORTH-WESTERN CENTRE, AT MANCHESTER, 7TH JANUARY, 1958*

Mr. J. A. Fox: In wind-tunnel work particularly, many aspects of the mechanical engineer's art have to be embraced by the electrical engineer. It is not sufficient for the mechanical engineer to design the shaft system to be free of mechanically inspired once-per-revolution oscillations in the drive operating range. It is also necessary to consider torsional excitations at these frequencies occurring in the operating speed range and emanating from the electrical side.

The number of torsional modes are, in fact, dependent on the number of separate masses on the shaft. The frequencies are a function of the masses and torsional rigidity of the shafts coupling them.

Since it is more convenient to have the drive motors outside the tunnel shell a long shaft is needed to provide connection to the compressor, and this arrangement tends to give a system which is not torsionally very rigid.

The induction-motor drive when operating with unbalanced rotor current develops a non-uniform torque, analytically comprising a positive uniform torque, a negative uniform torque and an oscillatory torque of twice the slip frequency. The amplitude of this oscillatory torque is dependent on the motor loading and degree of rotor-current unbalance and may be expressed as

$$T_m = \tfrac{2}{3}T_s\frac{I_{max}-I_{min}}{I_{max}}$$

where T_s is the positive uniform torque and I is the rotor phase current

It is necessary to assess theoretically and verify by test the degree of amplification occurring at the critical torsional frequencies for values of rotor unbalance, so that adequate safeguards can be provided to avoid dangerous oscillatory shear stresses in the drive.

Personal experience with a 20000 h.p. induction-motor wind-tunnel drive indicates that, while theoretical assessments of critical frequencies, exciting torque/rotor unbalance ratio and shaft damping had been accurately predicted, the presence of greater magnetic damping has led, in practice, to a much smaller amplifying factor at resonance than that calculated.

Owing to its similarity to the R.A.E. high-supersonic-speed tunnel I would like to have the authors' comments on the extent of their investigations; in particular, what is the first-node mode critical frequency, what amplification factor has been assigned to it; and, in general, what degree of electrical unbalance is theoretically unacceptable and what electrical or mechanical safeguards have been applied?

Mr. J. E. Eastham: Brushless variable-speed induction motors have been the subject of two recent papers† and are essentially low-speed multipolar machines. This means that they can be made only in large sizes if the efficiency is to be high.

The efficiency also depends on the demanded speed range, but a 2 : 1 range can be achieved without appreciable loss of efficiency in large machines.

The provision of a variable-frequency supply seems to meet exactly the requirements both for flexible variable-speed working at large powers and for peak lopping, provided that the R.A.E. requires several large wind tunnels and can afford the necessarily large capital cost.

However, if it is necessary to provide only a transonic tunnel of the type mentioned in Section 3.6 of Paper No. 2414 U using a composite drive, it may well be that a spherical brushless variable-speed induction motor could replace the slip-ring induction motor with considerable advantage. When designed to have a top speed of 485 r.p.m. the rotor efficiency of the spherical motor would be of the order of 80%, which is not much better than the wound-rotor machine running at 28% slip. If gearing is tolerable, however, then, by making the top speed 250 r.p.m. and demanding a speed range of 2 : 1, a rotor efficiency of 90% could be achieved. In addition to the considerable improvement in efficiency, the spherical machine has all the

* This discussion refers to Papers No. 2414 U and 2415 U only.

† WILLIAMS, F. C., and LAITHWAITE, E. R.: 'A Brushless Variable-Speed Induction Motor', *Proceedings I.E.E.*, Paper No. 1737 U, November, 1954 (102 A, p. 203).
WILLIAMS, F. C., LAITHWAITE, E. R., and PIGGOTT, L. S.: 'Brushless Variable-Speed Induction Motors', *Proceedings I.E.E.*, Paper No. 2097 U, June, 1956 (104 A, p. 102).

inherent advantages of a squirrel-cage motor. For example, there would be no brush-gear maintenance or torsional shaft oscillations due to rotor unbalance, and the temperature-controlled electrolytic resistance is eliminated.

Dr. E. Friedlander: The effect of temperature variation of the resistivity on the liquid controllers used is observed also in winder drives, but it is not difficult to compensate for large variations in the resistivity. This frequency is not only caused by varying temperature but also by varying soda content. Automatic compensation for these variations has been successfully introduced, and could possibly avoid the need for controlling the temperature of the electrolyte.

The authors refer to the power factor at reduced speed being poor with induction-motor drives particularly on 'non-cube law' drives. Generally, the power factor of an induction motor is essentially a function of its torque, so that a motor required to be started from standstill with full-load torque has an almost constant and reasonably good power factor over the whole speed range. The power factor at low speed is reduced if the torque is low. Therefore, it would appear that the disadvantage of the induction motor refers rather to efficiency than to a poor power factor at reduced speed if by 'non-cube-law' drives the authors mean those in which a large torque is required at low speeds.

A conceivable alternative to the modified Kramer system would be to use rectifiers either of the semi-conductor diode variety or mercury-arc rectifiers in the slip-ring circuit of the induction motor replacing the variable-frequency set. Would the authors propose to consider this in case of any future requirements of a similar kind?

In certain circumstances it is envisaged to synchronize the main driving motors by asynchronous operation from a 10 c/s supply. In a similar case for a.c. ship propulsion, it was found advantageous to rely for the starting and braking torques on the eddy currents in the solid pole shoes of the machine. It would be interesting to know whether a similar technique has been adopted in the case referred to by the authors.

Mr. J. N. M. Legate: I believe that, in mercury-arc rectifier schemes, the power factor deteriorates as the firing point is retarded. Would any advantage be obtained by the use of tapped-anode supply transformers to enable less grid phase shift to be used for reduced speed and/or load working for these applications?

I assume that tests are carried out at fairly steady speed and load conditions for some appreciable length of time, so that it might be possible to choose a suitably reduced anode voltage to enable a reasonable power factor to be obtained without unduly complicating the testing procedure.

Prof. F. C. Williams: The authors have described several drive systems employing machines of different ratings in tandem, but have not discussed the possibility of interconnecting a pair of machines through a differential gear. In tunnels where the required drive torque falls as the speed falls, such an arrangement has some advantages. If the fan is driven by the *difference* speed between a synchronous machine and a variable-speed machine having equal top speeds, both machines experience the same torque, but it is a reverse or generating torque in the case of the variable-speed machine, which therefore returns power to the mains.

If the tunnel is such that fan torque falls as speed falls, this arrangement results in a lower rating of the variable-speed machine. For example, if fan torque is proportional to speed, fan power is proportional to the square of fan speed. The power delivered by the constant-speed motor, which delivers forward torque proportional to fan speed, will be proportional to fan speed. The difference power is absorbed by the variable-speed machine acting as a generator. This difference has a maximum value at half speed and is then one-quarter of the maximum fan power. Maximum fan power is obtained wholly from the synchronous motor with the variable-speed machine stationary. With a cube-law fan the improvement is even greater. For starting, the machines can be run up to speed with the fan clamped stationary. The fan is then run up by slowing down the variable-speed machine. The synchronous motor can be used for power-factor correction of the variable-speed system. Not only is the rating of the variable-speed machine reduced, but its losses are, of course, reduced in proportion to the reduced power handled. Even if the variable-speed machine is replaced merely by an eddy-current brake, the losses are limited to 25% of maximum fan power. The figures are even more advantageous for 'cube law' fans.

THE AUTHORS' REPLIES TO THE ABOVE DISCUSSIONS

Messrs. P. McKearney, L. S. Drake and **E. G. Mallalieu** (*in reply*): Mr. Fletcher's contribution is valuable in illustrating the background against which the driving system for this project was developed. Experience with the a.c./d.c. composite drive has shown that, apart from running up the drive to full speed, which requires all the available 12 000 h.p., the d.c. component provides ideal speed control and, in addition, enables a whole series of tests to be undertaken when the a.c. power is required elsewhere. In fact, if these particular d.c. machines had not been available it would have been necessary to provide other motors of the same capacity with a speed-control system equivalent to that of the Ward Leonard in flexibility and range.

We agree with Mr. Thompson that a direct drive by a specially-designed steam-turbine installation might be feasible for an isolated tunnel drive. However, where a number of large drives have to be catered for, this would lead to a complete boiler/turbine installation for each tunnel drive, or, alternatively, a central boiler with steam mains several hundred feet long to the individual turbines, with a total prime-mover capacity several times that of the existing generating plant. In the present scheme the primary purpose of generation is to provide a comparatively small source of variable-frequency power capable of being switched as required to a number of drives, which, alternatively, can be run from the Grid system. Thus maximum use of the tunnel facilities can be arranged by suitable power distribution between the tunnels.

In reply to Mr. Fox, we have ascertained that the calculated frequency of the first mode of torsional vibration on the R.A.E. high-supersonic-speed tunnel is 296·8 cycles per minute. The exciting force will have this frequency when the slip of the induction motor is 4·95%. As the gear ratio between the induction motor and the synchronous motor is 931·6/3 000, this imposes a slip of 6·84% when the synchronous motor is connected to the same supply as the induction motor. Under this condition, the frequency of the exciting force is 38% above the natural frequency.

It follows, therefore, that this mode constitutes a possible hazard only if the set is over-speeded during the synchronizing operation. To cover this possibility a system of protection is being provided which gives warning if the slip frequency of the induction motor falls below a predetermined value or if the out-of-balance currents in the rotor phases of the induction motor exceed a predetermined value. If a combination of these conditions occurs the induction motor is tripped out.

The degree of unacceptable electrical unbalance in the rotor of the induction motor is that at which the torque variation in

the pinion shaft, due to the vibration, exceeds the main torque, thus causing separation of the gear teeth. Calculation of this amount of unbalance involves making certain assumptions about damping forces arising from energy dissipated in bearings, and hysteresis of shaft material. It has been considered preferable to confirm the frequency and the tolerable amount of unbalance experimentally and to set the protective gear to suit.

We have studied with interest Mr. Eastman's contribution and the papers to which he refers. It would appear that, at some later stage of development, the spherical induction motor will have to be considered seriously for variable-speed drives with limited speed range.

It is interesting to note that Dr. Friedlander confirms the necessity to control the soda content as well as the temperature of the electroyte in liquid controllers and that satisfactory automatic compensation for these variations has been achieved. We are aware of recent developments of medium-size Kramer drives using rectifiers to convert the slip energy of the induction motor for driving a d.c. motor coupled to the drive shaft, and consider that these drives may well merit consideration for future wind-tunnel work.

The normal method of running up and synchronizing the 8 ft tunnel drive under all conditions is by means of the d.c. motor, but, if the d.c. motor were not available, the a.c. motor could be run up to a synchronizing speed of 10 c/s on the damper windings in the pole faces.

Mr. Legate is correct in assuming that a tapped-anode supply transformer would enable the power factor of a grid-controlled rectifier to be adjusted. This effect has been noted on the R.A.E. rectifier drive when tap-changing on the site supply transformer. To extend this method to useful proportions on an installation of this size would involve a rather complex transformer arrangement.

Prof. Williams's suggested scheme of a composite drive through differential gearing shares with the Kramer system the disadvantage that for wind-tunnel duty, where a wide speed range and high torque at low speed are required, the variable-speed set becomes large and expensive. Such drives are then not competitive with other schemes, such as the Ward Leonard, which have the advantage of less complicated control.

Messrs. L. S. Drake, J. A. Fox and **G. H. A. Gunnell** (*in reply*): In reply to Mr. McTaggart, the detailed design of components and circuits for a high-accuracy control system may take several weeks and the time taken to check the open-loop frequency response is negligible. We agree that the closed-loop transient-response calculation is tedious, but nevertheless the result obtained from analysing the simple linear system is useful for checking the setting-up and functioning of the analogue computer. In the paper, the mathematics have been simplified by calculating the response due only to 'proportional control'. The advantage of using an analogue computer, apart from consideration of non-linearities, is mainly the ease with which the variable parameters may be optimized, after the original simulation has been proved correct. In practice, the transient response to a step of reference may be slightly modified by the load torque. This would tend to make the system less oscillatory.

In the control, which combined both Class 0 and Class 1 systems, the system gain m_1 was decided on 'proportional control' alone. The 'integral control' gain m_2 was fixed by design considerations of acceleration rate, etc., as well as for stability. For wind-tunnel drives the load torque at any given speed may be taken as approximately constant, but the rate of change of speed which results from changes in generator voltage due to temperature or frequency effects is dependent on load regulation. It has been found, practically, that, if the closed-loop regulation is made equal to the specified speed accuracy, the disturbances from outside the loop have a tolerable transient effect upon the system.

In reply to Mr. Thompson, we have shown that the Ward Leonard system, with its inherent low regulation, comprises the dominant control of the drive speed; the gas turbine, on the other hand, provides the major power. The full power of the Ward Leonard system can be employed at the same response rate for correction of speed errors above or below the set value, but this facility cannot be provided by a turbine, since excess power requires reduction of fuel, and consequent reduction of speed is obtained owing to load torque and turbine, losses only. In simple terms, the turbine possesses the unidirectional response of the d.c. system. While we agree that a frequency-sensitive device has several attractive features, we consider that the well-tried voltage-comparison system using a high-accuracy tacho-generator of robust design provides a reliable and simple system.

Messrs. T. Barnes and **C. R. Dunham** (*in reply*): In reply to Mr. Fletcher, the facility afforded by a control system in which the basic nozzle shapes may readily be altered was regarded, from the start, as being of considerable importance. It was one of the main reasons for adopting punched tape as the data store, in place of alternatives based on more mechanical principles, e.g. precision cams or templates. The feature has, in fact, proved its value, as mentioned in Section 7 of the paper. The task of preparing a new set of tapes is very much more easily undertaken than the alternative of producing 60 accurately shaped cams. The further facility of being able to make test changes to the nozzle shape by the manual insertion of impulses is likewise useful in establishing new data, and has proved well worth the small added complication to the circuits.

Whilst agreeing with Messrs. Scholes and Roberts that there are certain points which might have favoured a fully digital data-transmission scheme, we are inclined to the opinion that the extra apparatus necessary would have been difficult to justify. The situation might now be different, since so much more experience in this field is available. On the question of speed of operation, with the equipment as fitted the rate of impulsing is related to normal telegraph practice and this happens to give a rate of wall movement which nicely suits the hydraulic plant. Whereas it would be possible to achieve a higher speed of data transmission by using more up-to-date techniques, in the present tunnel other limitations preclude the need for an increased rate of wall travel.

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THE DEVELOPMENT AND OPERATION OF A 10 kW HOMOPOLAR GENERATOR WITH MERCURY BRUSHES

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SUMMARY

Principles of design and the experimental development of a 10 kW homopolar generator with mercury current-collector rings are described. The machine provides pure direct current ranging from 10 to 16 kA at 1·0–0·625 volt with an estimated efficiency of 91–88% respectively. It has a cylindrical copper rotor and a coaxial compensating conductor of magnetic material. The potentiality of this class of machine in much larger sizes is indicated.

LIST OF SYMBOLS

a = Length of arc of brush-liquid cross-section, cm.
B = Flux density in air-gap, gauss.
B_p = Mean circumferential component of flux density in iron return conductor, gauss.
B_r = Mean radial component of flux density in iron return conductor, gauss.
B_s = Average saturation flux density in iron return conductor, gauss.
d = Density of brush liquid, g/cm^3.
F_c = Magnetomotive force required for thickness t_c of copper rotor, ampere-turns.
F_s = Magnetomotive force required for mean effective width t_s of iron return conductor, ampere-turns.
g = Gravitational acceleration, cm/sec^2.
G = Ratio of mean centrifugal acceleration of brush liquid to gravitational acceleration.
h = Vertical difference in level between open surfaces of brush liquid, cm.
H_c = Maximum circumferential magnetic field strength at brush due to main current passing through brush, oersteds.
H_p = Mean value of circumferential magnetic field in iron return conductor due to armature current, oersteds.
H_r = Mean value of radial magnetic field in iron return conductor required to produce B_r, oersteds.
I = Output current of generator, amp.
I_m = Magnetizing current in field winding, amp.
l = Length of air-gap parallel to axis of machine, cm.
p_e = Pressure head of brush liquid derived from the interaction of the current and its field in the brush liquid, cm.
r = Mean radius of brush and generator rotor, cm.
Δr = Difference in radii of brush-liquid surfaces, cm.
r_s = Mean radius of iron return conductor, cm.
R = Total internal resistance of generator, ohms.
R_m = Resistance of field winding, ohms.
t_c = Thickness of copper rotor in air-gap, cm.
t_s = Mean effective width of iron return conductor, cm.
V = Terminal voltage of generator.
P = Ohmic losses due to passage of main current through generator, watts.
P_b = Combined brush friction loss at full-load speed, watts,
P_e = Ohmic loss due to circulating current in lower brush, watts.
P_f = Driving-belt loss, bearing friction loss and windage on light load, watts.
P_m = Ohmic loss in field winding, watts.
ρ_c = Resistivity of rotor copper when warm, ohm-cm.
ρ_s = Resistivity of low-carbon steel, ohm-cm.
ω = Mean angular speed of brush liquid, assumed to be half the rotor speed, rad/sec.

(1) INTRODUCTION

Homopolar machines received considerable attention when electrical engineering was becoming an established industry. The literature[1] contains many references to designs with solid brushes and mercury contacts, but until recent times the most successful machine to operate in an industrial application was the one developed by B. G. Lamme in the period 1906–12.[2] This used copper leaf brushes with 0·1–0·2-volt drop per brush under the best conditions. The machine ran at a speed of 1 200 r.p.m. and produced 2 MW, 7·7 kA at 260 volts. The story of its development makes fascinating reading. For large-scale power production the homopolar generator suffered the disadvantages of relatively low speed and d.c. output; alternators could be more readily matched to turbine speeds and the a.c. systems had the advantage of easy transformation for distribution. More recently a pulsed homopolar generator has been developed commercially for resistance welding;[3,4] this also uses solid brushes and conductors in slots. Several machines have been developed to supply power for research projects and these include pulsed generators with liquid-metal current collectors.[5,6] For continuous operation the chief interest to-day is in generators which can supply extremely heavy direct currents at only a few volts.

This demand has arisen with the application of electromagnetic pumping principles to the circulation of liquid-metal coolants in nuclear reactors. The d.c. pump has a minimum of insulation and maintenance problems and is therefore especially suited to pumping sodium or other alkali metals at temperatures not readily accommodated by a.c. pumps with polyphase windings. It is also well suited for pumping the high-resistivity liquid metals, such as bismuth, in a circulating fuel reactor; the efficiency of linear induction pumps is seriously reduced with these metals.[7] The overall efficiency is governed by those of the generator and pump combined, so it has been important to establish a source of current with an efficiency as near that of a conventional generator as possible and equally compact.

A design with rotor totally immersed in sodium-potassium alloy was developed in the United States by the late Dr. A. H. Barnes[8,9] at Argonne National Laboratory. The larger version of this is intended to produce about 250 kA at 2·5–3·0 volts for the d.c. pump (10 000 g.p.m., U.S. measure) which has been proposed for the EBR II reactor.[10] The generator efficiency is expected to exceed 80%. This machine has a minimum of brush problems because the current is conveyed from the rotor

Written contributions on papers published without being read at meetings are invited for consideration with a view to publication.
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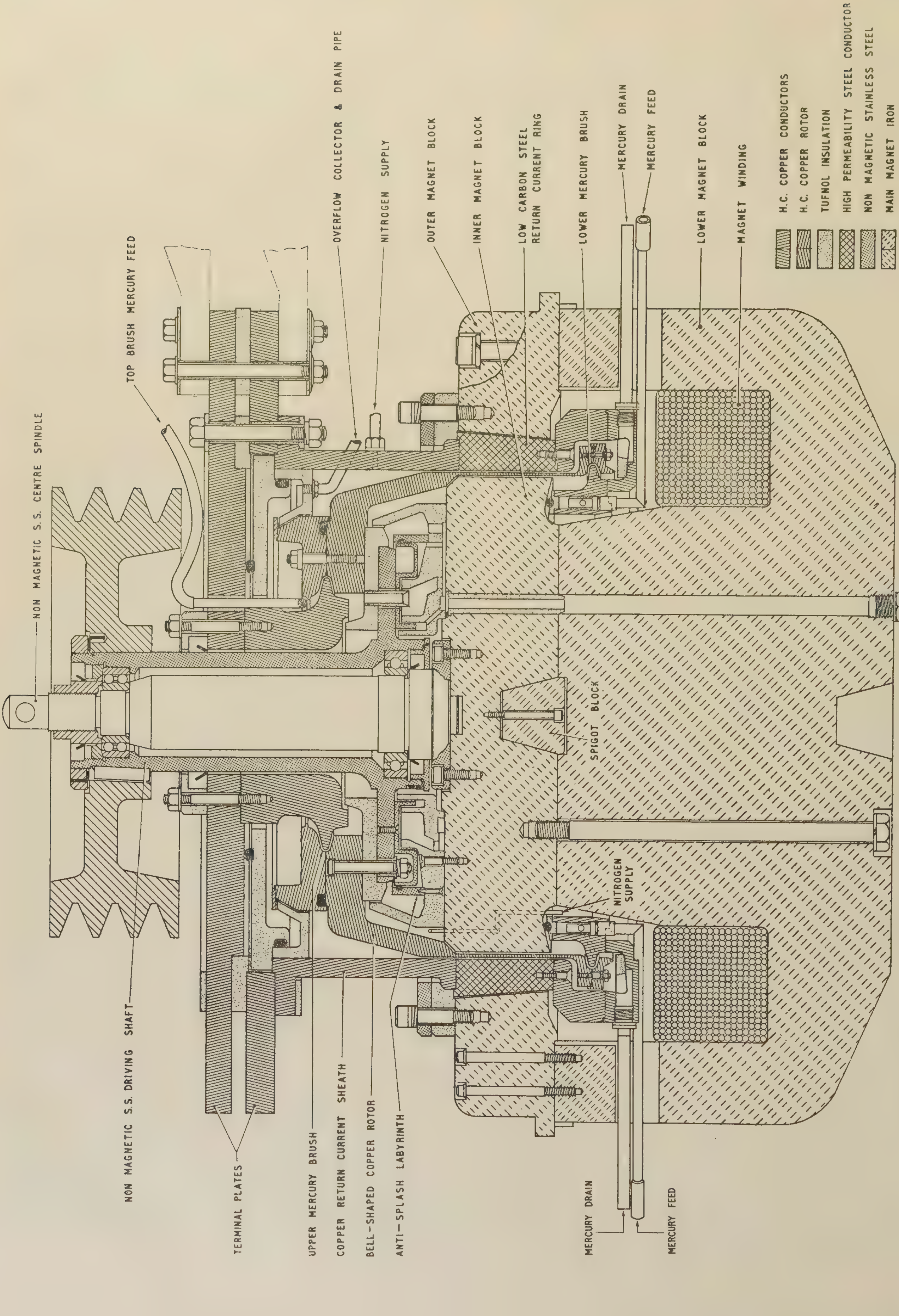

Fig. 1.—General arrangement of homopolar generator.

to the terminal conductors via the liquid-metal filling. The liquid is circulated by the pumping action derived from the rotor base, and the machine losses are removed by a heat exchanger in the liquid-metal circuit.

The present paper describes yet another approach to the development of highly efficient generators suitable for the d.c. electromagnetic pump or other processes requiring high current at low voltage. With liquid-metal brush rings it is possible to achieve 90% efficiency in small machines and more than 95% in ratings of about 100 kW. By careful design the brush friction and ohmic losses can be kept quite small, despite the current conveyed. The use of discrete collector rings eliminates the by-pass-current loss which occurs in an immersed rotor system. It also permits closer control of local eddy currents in the brushes.

(2) THE EXPERIMENTAL GENERATOR

Towards the end of 1950 it was decided to investigate possible brush systems and to build a small generator, of about 10 kW nominal rating, using liquid-metal collector rings or brushes. The range of output chosen from the design study was 10–16 kA at 1·0–0·625 volt.

(2.1) Description of Machine

For a small experimental machine a relatively light copper rotor was preferred. The cylindrical shape was chosen rather than the disc because a more compact design was made possible thereby, with brushes equally accessible, and because the thin cylinder has greater stiffness than the disc. Compensation can be simply effected by arranging a cylindrical return conductor coaxial with the rotor.

The general arrangement of the experimental generator is shown in Fig. 1. The cylindrical, bell-shaped, copper rotor is mounted on a fixed centre spindle with the cylindrical portion in the annular air-gap of a pot magnet. The machine rotates about a vertical axis. The lower mercury brush is shown at the rim of the rotor bell inside the magnet space, while the upper brush of smaller diameter is enclosed by the return-current sheath and terminal plates. The brushes and mercury system are purged and blanketed with nitrogen to prevent the formation of oxides, which would gradually block narrow flow-paths. Gaco oil-seal rings prevent outward flow of nitrogen and mercury vapour at the hollow shaft. The magnet winding, of 2 684 turns, lies in the base of the magnet block and requires about 2·0 amp at normal excitation. The lower magnet block and winding are removed when the lower brush is to be inspected. The overall dimensions of the machine as shown in Fig. 1 are: height 24 in, diameter 22 in, rotor diameter $12\frac{1}{2}$ in. Other views are shown in Figs. 2 and 3.

(2.2) Operation of Mercury Brushes

A small flow of mercury is supplied to both brushes to replace splashing losses and to remove some of the heat representing friction and resistance losses in the rotor and brushes. The machine is run up to normal speed before starting the mercury flow, since the operation of the top brush requires some centrifugal action to overcome surface-tension effects. In each brush the outer concave member rotates and the liquid metal overflows at the rim of larger diameter. The flow to the top brush is less than that required at the lower one, the friction losses being much smaller.

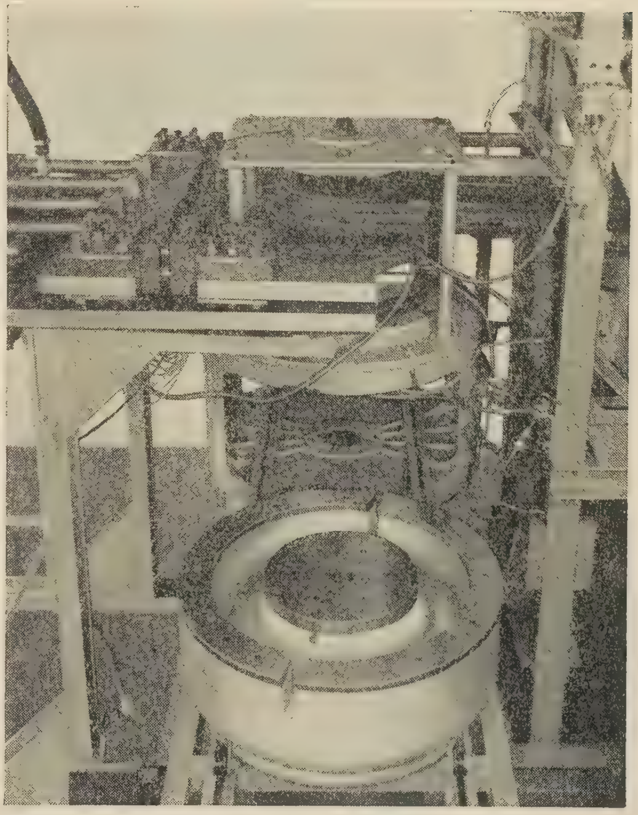

Fig. 2.—Generator with lower magnet block and winding removed for inspection of lower brush.

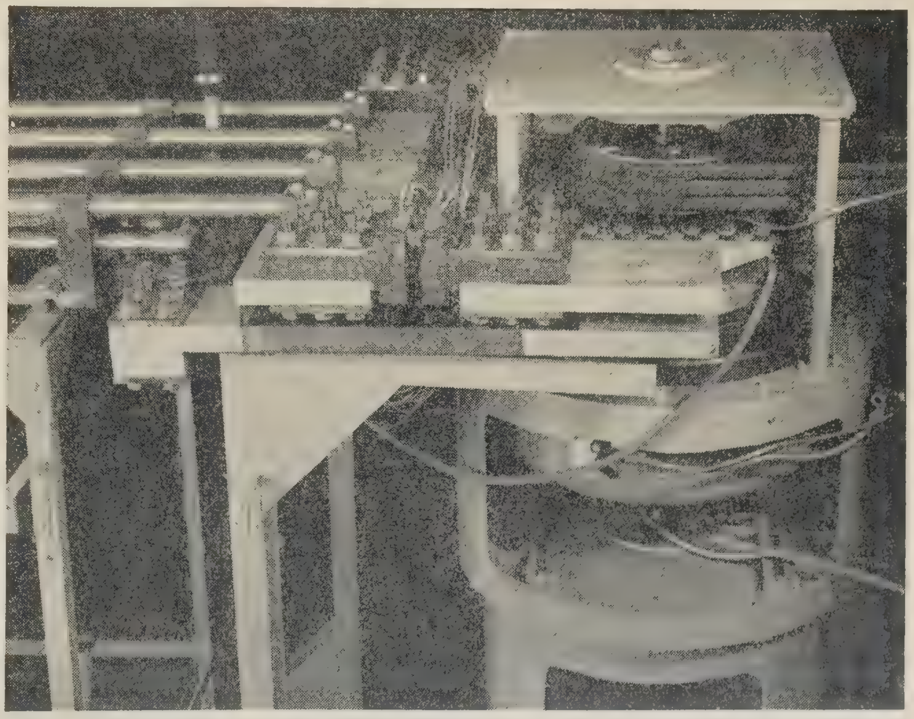

Fig. 3.—View showing terminal connections and heavy-current shunts.

The overflow collector and drain were installed as a precaution against splashing. Droplets of mercury would otherwise be carried into the narrow air-gaps where they would tend to accumulate and might eventually short-circuit the rotor. The rotor and pole faces were covered with a thin coating of epoxy casting resin to provide chemical protection and insulation.

Mercury normally flows through the top brush and is collected in the labyrinth under the rotor bell. At the bottom brush, mercury is fed through two $\frac{1}{8}$in-diameter holes in the stationary member of the brush. Instead of flowing from one surface to the other the mercury undergoes a combination of supply and overflow from the lower surface. The purpose of this is to minimize splashing at this brush. Provided that a sufficient head of mercury was maintained in the supply pipe the system was very satisfactory. There was no tendency for the jets to become blocked even when sludging occurred in the circular supply channel behind the brush.

(3) DEVELOPMENT OF THE BRUSH SYSTEM

(3.1) Friction Measurements and Shape of Brush Channel

A prolonged series of model tests was carried out[11] to determine optimum shapes of brush channel and to study the behaviour of the liquid. The results of these tests were incorporated in the full design study of the experimental generator.[12]

Mercury and sodium-potassium alloy were both considered for use as brush liquids, and the friction tests were made with mercury and water, which corresponds fairly closely to sodium-potassium eutectic alloy in hydrodynamic properties. Curved and rectangular channels of various widths were tried, from 0·010in upwards. It was found that the more satisfactory result was obtained with the outer hollow member of the brush pair rotating and the inner one fixed. Friction losses appeared to reach a minimum with a channel width of $\frac{1}{16}$in, regardless of shape of section, and for a given speed of rotation were proportional to the quantity of mercury in the channel. The index of the torque/speed law was 1·7–1·85 for the cases considered.

A further reason for preferring a brush channel width of $\frac{1}{16}$in to the narrower channels tested is that at the lower brush of the generator the liquid filling itself constitutes the principal resistance to eddy currents in the brush. These arise through the motion of the brush element in the fringing and leakage field of the magnet. For a given contact area in the brush channel the circulating current and power loss will vary inversely with the thickness of liquid-metal filling. Mercury will have the advantage over sodium-potassium alloy on account of its greater resistivity—approximately $2\frac{1}{2}$ times that of alloy and 50 times that of copper at room temperature. The ohmic losses due to the passage of the main current through the brush were estimated to be smaller than the friction and possible circulating-current losses.

(3.2) Brush Stability when Conducting Current

When a heavy current passes through the liquid in the brush there arise electromagnetic forces which tend to displace or even eject the liquid metal from the brush channel. These forces are due to the interaction of the current and its own field. The movement of liquid levels is always in the direction that will enlarge the cross-sectional area of the loop formed by the brushes, rotor and stationary conductors. Thus in the arrangement shown in Fig. 1 the displacement is downward at the upper brush and upward at the lower brush. In calculating the electromagnetic force and displacement, circular symmetry is assumed and the liquid is treated as if it experienced the full field of the current conveyed. For uniform current density the mean field experienced by the liquid currents is half this value, but where local circulating currents occur in the brush the current density is non-uniform and there is an additional component of force. In Fig. 1 the maximum current density in the lower brush will occur towards the region of full circumferential field. A complete theory would take account of the interaction of the main current with its own field and the interaction of the local circulating current with this field. In a symmetrical system the circumferential field of the eddy currents will produce no net force with these currents or the main current superimposed. In a simple brush of symmetrical cross-section it can be shown that, provided the magnitude of the circulating current in the brush does not exceed twice the main current through the brush, the displacement force will not exceed the value given by ignoring the eddy current and treating the main current as if it operated in the full value of its circumferential field. This is a reasonable assumption for estimating the limits of safe operation in a practical design.

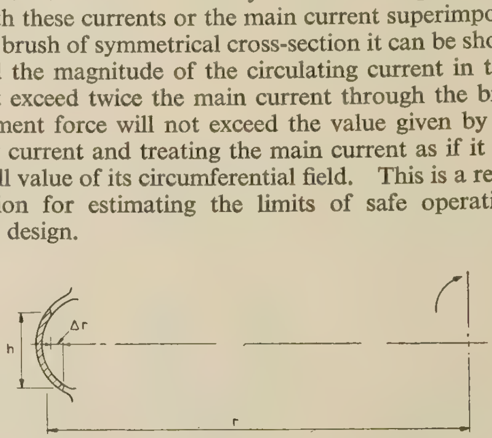

Fig. 4.—Equilibrium of brush liquid.

Consider the arrangement shown in Fig. 4. Using the symbols already listed, we have

$$H_c = \frac{2I}{10r} \quad . \quad . \quad . \quad . \quad . \quad . \quad (1)$$

Hence the assumed value of displacement pressure is

$$\frac{H_c I}{20\pi r} = \frac{I^2}{100\pi r^2} \text{ dynes per square centimetre} \quad . \quad . \quad (2)$$

The difference of pressure due to the vertical interval h is

$$dgh \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad (3)$$

and the difference of pressure due to centrifugal action is

$$\omega^2 r \Delta r d \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad (4)$$

This equation will apply provided the channel is quite narrow. The value of ω can usually be taken as half the angular speed of the rotor.

At the lower brush in Fig. 1 the electromagnetic and gravitational forces are in opposition, and the displacement of the liquid metal will be given by

$$dgh - \frac{I^2}{100\pi r^2} = \omega^2 r \Delta r d \quad . \quad . \quad . \quad . \quad (5)$$

At the upper brush the gravitational and electromagnetic forces act together, so that

$$dgh + \frac{I^2}{100\pi r^2} = \omega^2 r \Delta r d \quad . \quad . \quad . \quad . \quad (6)$$

It is assumed that the surface pressures are equal, but this may not be so in practice since the brushes are themselves a type of seal; the pressures could be made to differ intentionally.

Assuming equal pressures, we have in general

$$\Delta r = \frac{gh \pm \dfrac{I^2}{100\pi r^2 d}}{\omega^2 r} \quad . \quad . \quad . \quad . \quad . \quad . \quad (7)$$

or

$$\Delta r = \frac{h}{G} \pm \frac{p_e}{G} \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad (8)$$

where G, the ratio of mean liquid centrifugal acceleration to gravitational acceleration, is given by

$$G = \frac{\omega^2 r}{g} \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad (9)$$

and p_e, the pressure head due to the electromagnetic forces, is given by

$$p_e = \frac{I^2}{100\pi r^2 dg} \quad . \quad . \quad . \quad . \quad . \quad . \quad (10)$$

Referring to Fig. 4, the limit of stable operation is reached when the length of arc of liquid cross-section, a, lies entirely above or below the centre-line. In a brush with a radially deep rectangular channel the unstable condition occurs when Δr is very nearly equal to a. For safe operation Δr must be small compared with a.

Since the displacement due to electromagnetic forces varies inversely with liquid density and the square of angular speed, the speed required with sodium-potassium alloy in a given brush will be about four times that needed with mercury in order to have the same measure of stability. This will be found to imply greater friction losses than with mercury. In the overall design study[12] it was found that the optimum designs of machines for a satisfactory measure of brush stability with alloy brushes were little different in efficiency from optimum designs using mercury. Mercury was considered to be the preferred liquid for these reasons and because it is so much easier to handle in experimental equipment.

The theory of brush stability was checked in a small model brush system without magnetic core (Fig. 5). This is shown in diagrammatic form in Fig. 6. The mean radius r was 5 cm and the brush channels were semicircular, the section being of 0·5 cm radius. Using eqn. (7), the estimated maximum difference of surface radii, Δr, was 3·0 mm with 10·5 kA direct current passing through the brushes. No untoward effects occurred in a test of about one minute duration at this current. The voltage measured across the device was about 10 mV; the brush-channel surfaces were amalgamated copper. The rotor speed was 700 r.p.m., corresponding to the lowest possible speed in the experimental generator. Even at the smaller upper brush of the machine the conditions are less severe than in the model. The estimated maximum possible radial difference, Δr, is about 1·5 mm at 16 kA, 725 r.p.m. At the lower brush the maximum radial difference given by eqn. (7) is about 0·3 mm, taking the electromagnetic forces in opposition to the gravitational, which predominate in this case.

The safe upper limit of current in the brushes is governed by conditions in the upper brush. Taking the radial depth of channel on the lower side as 1·0 cm, the axial depth of mercury at the limit of stability as 0·5 cm, the mean radius of brush as 3·5 cm, and mean angular speed of the fluid on load as 363 r.p.m., eqn. (7) gives an upper limit of 60 kA. Since the leakage field through this brush is very small, the circulating component of current will be small and the average effective circumferential field approximates to $\frac{1}{2}H_c$. With this assumption the limit of stability is increased by $\sqrt{2}$ to 85 kA. For the lower brush the limit given by eqn. (7) is about 145 kA. The overload safety margin for brushes intended to carry a maximum of 56 kA is therefore ample at the normal running speed.

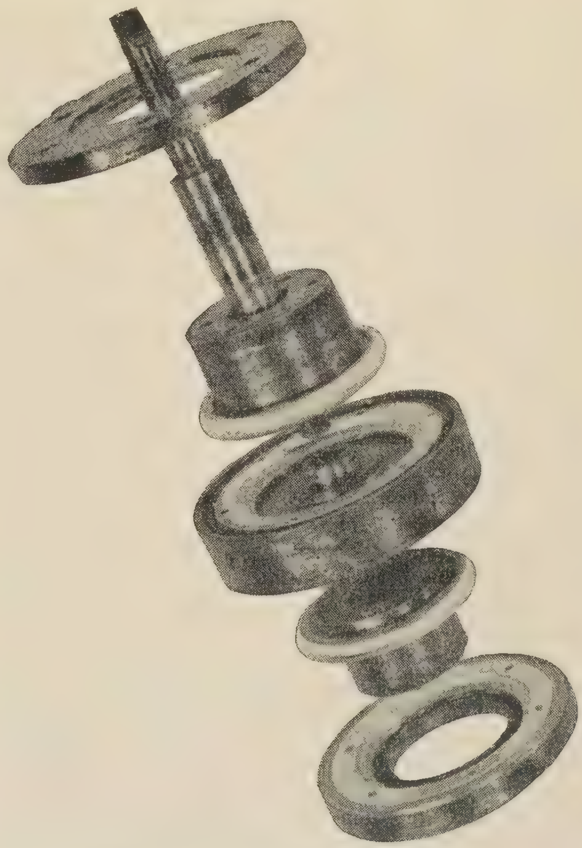

Fig. 5.—Exploded view of model brushes.

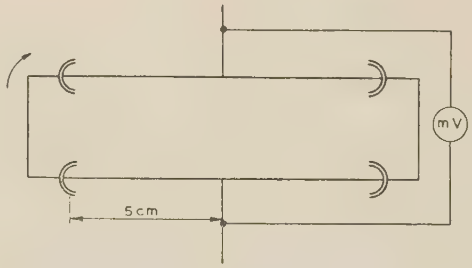

Fig. 6.—Diagram of model brush assembly.

A serious short-circuit is unlikely to occur with such low-voltage high-current ratings, but the generator field would presumably be interlocked with the driving motor, which would be the first item to trip. Even if the change in field is small the voltage and current will decline with the speed and so the ratio of electromagnetic and centrifugal effects in the brushes will tend to remain constant as the speed falls. At very low speeds the voltages and currents would be much reduced, and if the brushes did open circuit the risk of damage to heavily plated surfaces is small.

(3.3) Brush Materials

Experience with the model brush assembly had suggested that corrosion of plain copper with mercury might not be very serious, and preliminary tests with thin plating revealed no serious deficiencies. Conditions at the lower brush of the generator, however, were much more severe. The peripheral speed of the moving member exceeds 40 ft/s. Some of the original copper

components had to be replaced and the protection finally adopted was a heavy plating of nickel on copper, with a rhodium finish. This has withstood extensive tests. Rhodium makes excellent electrical contact with mercury because it is free of protective oxide films and is itself insoluble in mercury. Under operating conditions the mercury temperature in the brushes is kept within a limit of about 30° C. Mild-steel brushes could be used, but it is preferable to plate the surfaces with nickel and rhodium in order to avoid rusting at any time and subsequent fouling of mercury.

(4) COMPENSATING CONDUCTOR OF MAGNETIC MATERIAL

If magnetic material is used for the current paths adjacent to the air-gap of a homopolar machine the magnetizing force required can be much less than that needed for continuous copper conductors. A system using copper bars embedded in the magnet material would have least reluctance and resistance but is obviously more complicated to construct. With exceedingly heavy currents, say 250 kA, at 1·0 or 2·0 volts, it would undoubtedly be worth while, but in small machines the gain in efficiency is trivial, as can be judged from the figures for losses given below.

In the experimental generator the rotor is of high-conductivity copper with a thickness, t_c, of 3·5 mm, and the return conductor is a low-carbon steel ring, with an effective thickness, t_s, of 2·1 cm. When delivering 10 kA at 1·0 volt and 725 r.p.m., the air-gap flux density is about 13 kG. Taking the resistivity of the copper, ρ_c, as 2·0 microhm-cm and that of the low-carbon steel, ρ_s, as 12·0 microhm-cm, the mean radius of the rotor, r, as 16·0 cm and that of the return current ring, r_s, as 17·3 cm, under steady running conditions the ohmic losses are as follows:

$$\text{Loss in 6 cm length, } l\text{, of rotor} = \frac{l}{2\pi r t_c}\rho_c I^2 = 34\cdot 0 \text{ watts.}$$

$$\text{Loss in 6 cm length, } l\text{, of steel ring} = \frac{l}{2\pi r_s t_s}\rho_s I^2 = 31\cdot 5 \text{ watts.}$$

The resistances and ohmic losses are clearly comparable.

With an air-gap flux density of 13 kG the m.m.f. required for the copper cylinder is

$$F_c = \frac{10}{4\pi}Bt_c = 3\,620 \text{ ampere-turns} \quad . \quad . \quad . \quad (11)$$

The mean radial flux density in the compensating ring is

$$B_r = B\frac{r}{r_s} = 12 \text{ kilogauss} \quad . \quad . \quad . \quad . \quad (12)$$

The mean value of the circumferential magnetizing force is

$$H_p = \frac{1}{2}\frac{2I}{10r_s} = \frac{I}{10r_s} \quad . \quad . \quad . \quad . \quad (13)$$

very nearly.

At a load of 10 kA the mean value of H_p is 57·8 oersteds. Since the ring presents a continuous iron circuit to the field H_p, it will be at or near magnetic saturation over most of its radial width. If B_s denotes the saturation flux density, the circumferential component of flux density B_p will be given by

$$B_p = \sqrt{(B_s^2 - B_r^2)} \quad . \quad . \quad . \quad . \quad . \quad (14)$$

The components B_r and B_p are in the same proportion as H_r and H_p; i.e.

$$\frac{H_r}{H_p} = \frac{B_r}{B_p} = \frac{B_r}{\sqrt{(B_s^2 - B_r^2)}} \quad . \quad . \quad . \quad . \quad (15)$$

In the simplified case, B_r is assumed constant across the width t_s of the ring, and the material is treated as if it were at a fixed value of flux density B_s throughout. In a thin ring H_p varies linearly from zero at one side of the ring to a maximum at the other. Thus, given B_r, B_s and the mean value of H_p, the mean value of H_r can be deduced.

Taking the probable average value of B_s as 17·5 kG and substituting the mean values given above for B_r and H_p at $I = 10$ kA, the mean value of H_r is

$$H_r = H_p\frac{B_r}{\sqrt{(B_s^2 - B_r^2)}} = 54\cdot 4 \text{ oersteds} \quad . \quad . \quad (16)$$

Hence, the m.m.f. required for the low-carbon steel ring is given by

$$F_s = \frac{10}{4\pi}H_r t_s = 91 \text{ ampere-turns} \quad . \quad . \quad . \quad (17)$$

This compares with 3 620 ampere-turns for the copper rotor. The assumption of a constant value of B_s leads to a slight over-estimation of the mean value of H_r, especially if the choice of B_s is conservative. A step-by-step method, taking narrow annuli of the iron ring, would yield a more accurate result but is hardly necessary in this design.

In the other case of 16 kA at 0·625 volt and 725 r.p.m., the air-gap flux density B is 9·5 kG, approximately. The m.m.f. required for the copper of the rotor is $F_c = (10/4\pi)Bt_c = 2\,645$ ampere-turns. The mean radial flux density in the compensating ring $B_r = Br/r_s = 8\,780$ gauss. At 16 kA the mean value of H_p in the ring is quite closely $I/10r_s = 92\cdot 5$ oersteds. The probable average value of $B_s = 18$ kG. Hence the mean value of H_r, by eqn. (16), is 51·6 oersteds. The m.m.f. required for the iron ring will be

$$F_s = \frac{10}{4\pi}H_r t_s = 86\cdot 4 \text{ ampere-turns}$$

This is practically the same as the value for F_s for the 10 kA load at 1·0 volt, and the advantage compared with the copper rotor is again considerable.

In the experimental machine the compensating ring is insulated from the main magnet iron. Where the magnetic circuit makes contact with only one side of the generator output the return-current circuit can be through the magnet material. An insulated ring need not be used, but the current must be taken away through a circular band of material, as in the present machine. The resistance of this portion of the main-current path would be greatly reduced and the current capacity of the machine increased. An uncompensated machine would be one in which the current was taken from within the magnet via a conductor or conductors which passed through the iron remote from the main air-gap. In general this will result in a disturbance in air-gap flux distribution on load; an ingenious and useful exception has been devised by Mr. J. M. Raven.[13]

(5) PERFORMANCE OF THE EXPERIMENTAL GENERATOR

The excitation characteristic is given in Fig. 7, and curves of efficiency against current for the 10 kW output rating are given in Fig. 8. These were calculated on a basis of average losses. Belt and bearing friction varied with belt tension, and the brush friction and eddy-current losses varied with the rate of supply of mercury through the jets. The field losses were derived directly from voltage and current measurements, while the internal resistance of the generator was derived from an estimation of the regulation at full power and different voltage/current ratings. This was checked by direct calculation as far as possible,

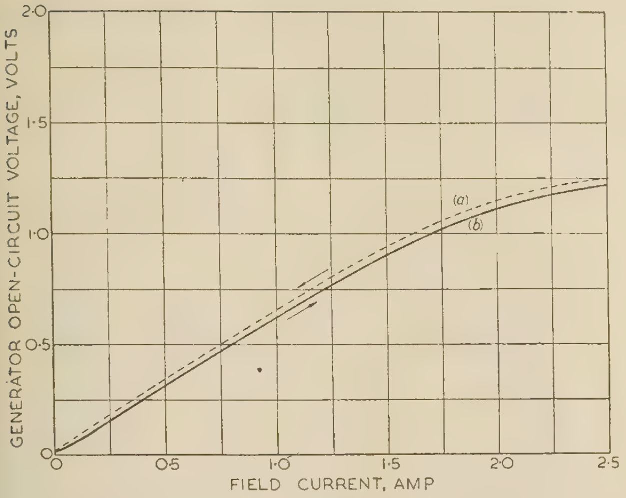

Fig. 7.—Generator excitation characteristic.

(a) Demagnetization from saturation, $I_m = 5$ amp.
(b) Excitation following demagnetization as in (a).
Speed 765–770 r.p.m.

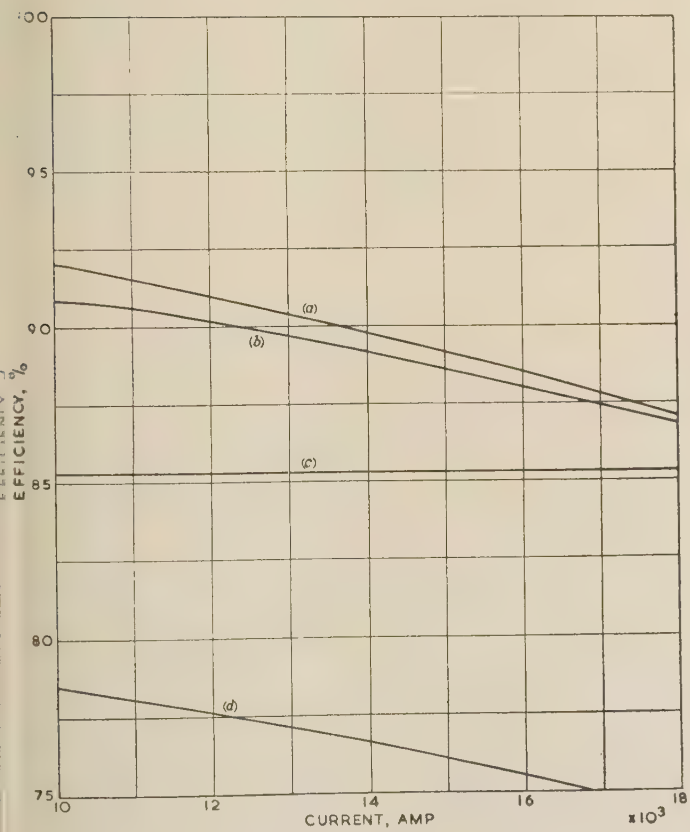

Fig. 8.—Efficiency/current characteristics at 10kW output.

(a) Generator efficiency, field losses neglected.
(b) Generator efficiency, including excitation power.
(c) Average drive efficiency, derived from (a) and (d).
(d) Conversion efficiency (generator output/motor input), A.C. to D.C.

although the resistance of the bolted joints introduced a margin of uncertainty. The joint resistances amounted to about 35% of the total internal resistance, the brushes contributing only 10%.

The values are set out below:

Belt losses, bearing friction and windage on light load, P_f	300 watts.
Lower-brush mercury friction, open-circuit	200 watts.
Upper-brush mercury friction, open-circuit	30 watts.
Generator speed, open-circuit	765–770 r.p.m.
Generator speed, full load	725 r.p.m.
Combined brush-friction loss at 725 r.p.m., P_b	200 watts.
Assumed values of circulating-current power loss at lower brush, P_e	75 watts at 1·0 volt, 35 watts at 0·62 volt.
Field winding resistance, R_m	37·0 ohms.
Range of field current, I_m	2·0–1·2 amp.
Range of field winding loss, P_m	148–53 watts.
Internal resistance of generator between terminal plates, R	$3\cdot0 \pm 0\cdot5$ microhms.

The brushes contribute about 0·3 microhm and solid materials about 1·7 microhms, the remainder being joint resistance.

With the mean value of R, the main ohmic losses, P, are 300 watts at 10kA and 770 watts at 16kA.

In the original design study, operation at a higher speed was envisaged for the 1·0 volt, 10kA rating, but with the return conductor of magnetic material the higher voltage was obtained at the lower speed with normal current in the field winding. The brush-friction losses were measured by observing carefully the change in motor input as the brushes were filled, with the generator at zero excitation. On light load the change in motor losses could be neglected. The circulating-current losses were measured by observing the further small increase in motor input as the generator was excited on open-circuit. If the brushes were empty it was found that no increase in motor input occurred when the field winding was energized, an indication that there were negligible circulating currents in the copper rotor. In deriving the full-load regulation the generator was always de-excited from 5·0 amp field current and the voltage was then measured as the field current was raised step by step from zero. The excitation curve was taken in the same manner so that the open-circuit and full-load voltages would correspond without any difference attributable to hysteresis. Corrections were applied for the change of speed on load and for the change of reluctance of the magnetic return conductor. Values of generator resistance obtained in this way varied from 2·0 to 3·5 microhms owing to the large effect of experimental error on the small voltage differences involved. The curves of generator efficiency were computed from the data listed above, but the curve of overall conversion efficiency, from motor input to generator output, is based on experimental figures for a.c. and d.c. power. The average drive efficiency was derived by taking overall efficiency as a fraction of generator efficiency.

(6) CONCLUSION

The successful operation of a 10kW generator delivering a current of 10–16kA with brush losses of about 300 watts has established a new range of efficient low-voltage, high-current d.c. machine designs. In a design with magnetic material for the rotor, preferably integral with the centre pole, which itself rotates, the internal resistance could be reduced to one microhm or less. A machine of 100kW rating could have an efficiency of at least 95% although delivering current as high as 50kA at a mere 2·0 volts. An overall conversion efficiency of d.c. output against a.c. supply approaching 90% can be envisaged even at these voltages. Industrial applications of low-voltage direct current usually require a somewhat higher voltage, and by combining machines in a back-to-back arrangement of cores and rotor circuits the voltage from a given diameter of machine can be doubled. Further development of brushes

should lead to increased brush speeds and so permit a higher voltage from a given size of machine. On the basis of present data two generators series-connected could be designed to produce, say, 50 kA at 7·0 volts with a brush speed of 45 ft/s.

These machines have no commutator and consist of relatively simple parts with a minimum of insulation requirements, and it is therefore to be expected that constructional costs will be less than those for a higher-voltage commutator machine delivering equal power. From the operational standpoint maintenance of a fully developed system can be expected to be less because the continuous circulation of clean mercury replaces the periodic clean-up of d.c. commutators. The mercury system would normally operate with quite a small temperature rise, and suitable running seals would ensure that the escape of vapour was insignificant.

(7) ACKNOWLEDGMENTS

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TRANSFORMERS, REGULATORS AND REACTORS

A Review of Progress

By E. T. NORRIS, Member.

(1) INTRODUCTION

The design and construction of transformers and associated apparatus have progressed steadily and well since the last progress review,* without any changes that could be termed revolutionary or epoch-making.

Within this period the 275 kV Grid system has been inaugurated. Transformers in the 300–400 kV range are being built by British manufacturers.

Following the comparatively sudden increase in turbo-generator ratings due to the forced internal cooling of conductors, transformer sizes have risen to 500 MVA and even higher. This increase has accentuated difficulties due to mechanical stresses and transport limitations. Grain-oriented cold-rolled steel has come into general industrial use as distinct from the limited application described in the last review. In particular, the production of wound cores for small rural transformers is now common. The constructional standardization of distribution transformers by the late Central Electricity Authority and the Area Boards has greatly simplified their manufacture with consequent economy and interchangeability. These are some of the outstanding developments which will appear in more detail in the course of this review.

It is proposed to consider first certain characteristics important (in greater or lesser degree) to transformers in general and then to deal more particularly with different classes of transformers and their applications, including regulators and reactors.

(2) GENERAL CHARACTERISTICS

(2.1) Grain-Oriented Cold-Rolled Steel

The outstanding development in the period under review, owing to improvement in material, results from the use of grain-oriented steel. The advantages were dealt with in the last review, where it was stated that only limited supplies were then available. It is now in large-scale commercial production and is being used to a significant extent by all transformer manufacturers. For a given flux density its loss is less than half that of hot-rolled steel. Considerably higher flux densities can thus be employed and still result in a notable loss reduction. The upper limit of flux density will probably be set by harmonics and inrush magnetizing current since the saturation flux density is not appreciably increased.

In general, therefore, the use of grain-oriented steel has promoted reductions in both the weight and size of the transformer and in both its iron and copper losses.

In order to make the best use of the directional qualities due to grain orientation, stress-relief annealing is essential following all cutting operations. Continuous annealing furnaces are now available with improved uniformity of treatment and continuous production. The ordinary interleaved joint is tending to be replaced by some form of mitred joint for the larger transformers and by cores wound continuously or with some form of overlapping joint in the smaller transformers.[19]

* Norris, E. T.: 'Electrical Plant and Machinery: Transformers, Regulators and Reactors', *Proceedings I.E.E.*, Paper No. 1245, March, 1952 (99, Part I, p. 68).

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(2.2) Impulse Strength of Windings

The principle of basic insulation levels described in the last review is now established on a world-wide basis. Standard levels have been incorporated in a publication of the International Electrotechnical Commission.[8, 10] In 1956 the transformer section of B.E.A.M.A. issued recommendations[7] covering both insulation levels, power frequency and impulse tests intended to serve as an interim measure until the publication of the revision of B.S. 171: 1936.

The standard impulse tests are based upon lightning surges travelling along the line into the transformer either as a full wave or chopped by flashover. The increasing use of compressed-air circuit-breakers has drawn attention to the possible danger of switching surges. The characteristics of these surges are being studied in many countries.

The neutral-current method of detecting breakdown during an impulse test described in the last review is now in general use for full-wave testing. The most important development in recent years has been the means of obtaining chopped waves with precise timing, which enables the neutral-current method to be applied to chopped waves as well as full waves.[4, 5, 6] Hitherto, reliance has had to be placed largely upon repeating a full-wave test after the chopped waves, which is a somewhat uncertain means of detection. It is now possible to determine with assurance any failure or partial failure in the insulation due to either chopped or full waves.

Since transformer-winding insulation, particularly between coils and turns, is usually determined more by impulse stresses than by power-frequency stresses, considerable study has been given to the surge strength of transformer insulation. The National Physical Laboratory[2] and the E.R.A.[9] have made important contributions to this study.

(2.3) Insulation

As stated in the two previous reviews, paper and pressboard are still the principal insulating materials in oil-immersed transformers. For the lower voltages a varnish impregnation may be used, chiefly for structural and protective sealing purposes, but for the higher voltages the oil also forms the impregnating medium. In this respect there has been little change in transformer insulation design over many years.

The importance of complete impregnation has been accentuated by the growth of impulse testing. It is no longer possible to rely upon time to complete the impregnation. Heat-treatment processes have been given detailed attention, accompanied by the measurement of power factor, insulation resistance or dispersion.[13]

The general use of paper-covered conductors has created a demand for a standard which has been met by the issue of B.S. 2776: 1956.[11]

For the smaller transformers (and also sometimes for strand insulation in multi-strand conductors) one of the newer synthetic enamels, such as polyvinyl acetal, is used as conductor covering—usually reinforced by paper insulation. The use of high-temperature synthetic materials such as one of the silicones, glass fibre, etc., is confined to dry-type transformers and deal with in Section 5.

(2.4) Transformer Noise

The increasing size of transformers in suburban areas and the growing difficulty of finding sites for new substations remote from residential property combine to make the problem of transformer noise both ever-present and serious. This is an international problem and much study is being devoted to it.[14, 15, 16]

The noise generated by a transformer can obviously be reduced by lowering the flux density. However, this is not an economical solution in itself. The noise is caused partly by magnetostriction (which is an inherent characteristic of the core material) and partly by transverse vibrations of the laminations. Assuming correct design of the clamping and the core section, this latter component is thought to be caused largely by waviness of the sheets and by cross fluxes between laminations due to large variations in the permeability of different sheets and also in different parts of the same sheet. It is likely that these effects will be improved in the near future when continuous strip steel is commercially available in this country.

The alternative treatment is to shield the transformer itself by walls, sound enclosure or buildings. According to the degree of sound insulation achieved, noise reductions of up to 30 dB can be obtained. An economical solution to avoid the cost of a complete building for transformers with separate cooling banks is a weatherproof blanket for the transformer tank,[15] the bushings being allowed to protrude. This is a revival of a method adopted some years ago.[17] The average noise reduction is 16–20 dB. Fig. 1 shows such a construction developed by the Central Electricity Generating Board.

corresponding to different classes of loading—rural, domestic, industrial, etc. It is hoped that the results in charts and tables will be of more practical help to operating engineers in making full use of the thermal capacity of transformers under various practical conditions of loading and ambient temperature.

(2.6) Aluminium Windings

The price of copper has fluctuated erratically over a wide range in recent years, and at its maximum has approached the limit at which it can be challenged economically by aluminium as a conductor for transformer windings. Consequently transformer manufacturers generally have been studying the technique of using aluminium, and in many cases have built prototype transformers (both large and small) with aluminium windings in order to establish methods of winding, insulation, jointing, etc., and so be prepared for immediate change-over to aluminium should it be warranted at any time by the price ratio of the two metals.[18]

In general, aluminium has no marked advantage in efficiency, weight or dimensions. The choice will therefore be determined by economic considerations.

The insulating properties of anodized aluminium have long been a lure to transformer designers,[19] since the thermal characteristics of the anodic layer and its thinness make it superficially attractive. However, there are practical difficulties to be resolved, and as yet it is not in general use. For small transformers there is the further possibility of its use on foil for multi-layer coils, and this application has received some attention recently in America.

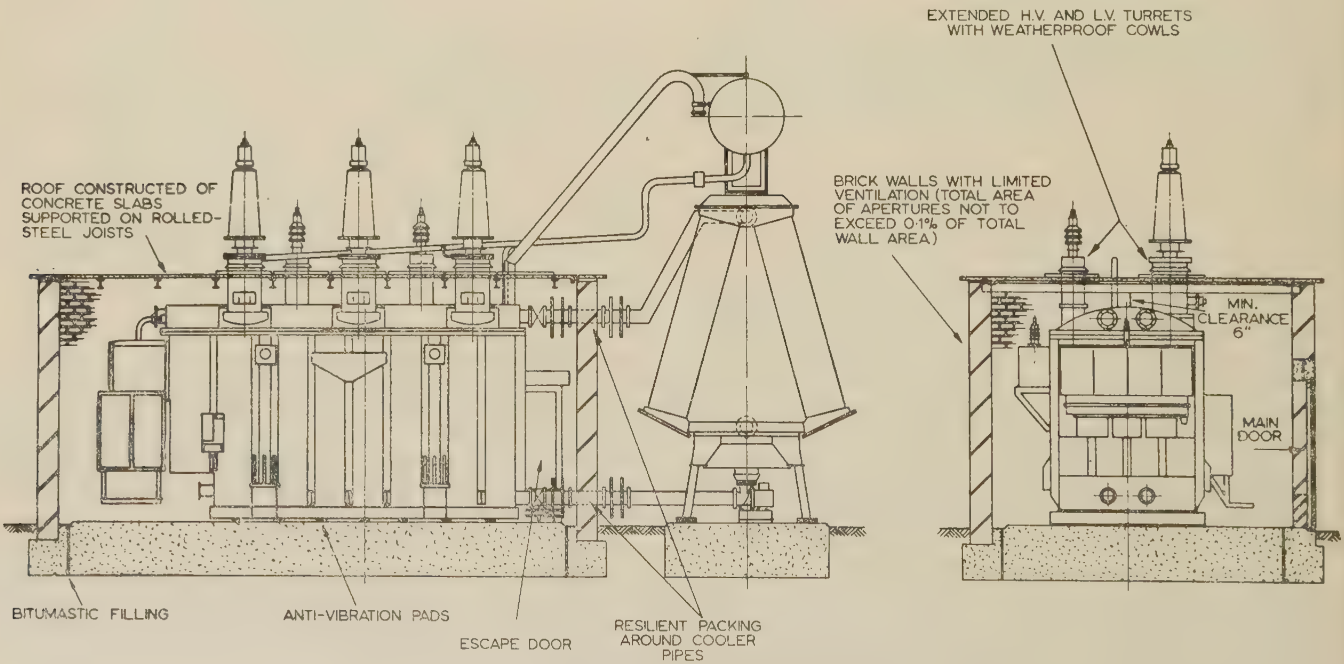

Fig. 1.—Sound insulating enclosure for a large transformer with separate cooling bank.

(2.5) Thermal Rating

The principles of thermal rating and overload capacity based on the maximum permissible hottest-spot temperature of the insulation are now well established. There have been no changes of major importance for oil-immersed transformers since the last review.

It is likely that the next revision of B.S. 171 will include more comprehensive overloading data based on realistic loading cycles

(2.7) Digital Computers

The designers' work may be divided into two parts. First, there is the design proper, and this includes

(*a*) The choice of the most suitable materials and of limiting mechanical, electrical and thermal stresses in all parts of these materials and under working or test conditions.

(*b*) The development of a practical means of determining these stresses for any particular construction.

This is the essence of design and is the fundamental part of the designers' work. It can be done by none but skilled designers.

The second part consists in calculating numerical values of all design characteristics for a given specification, and repeating the process by trial and error until the combination has been reached which satisfies the specification requirements whilst meeting all the stress limitations. The number of trials required ('iterations' is the modern term) depends on the experience of the designer, who can frequently make a shrewd and accurate initial guess which needs merely minor adjustments. The work is purely arithmetical, and, if the number of iterations is ignored, needs no design skill or engineering knowledge. It could be done by junior non-technical assistants were it not that the number of trials or iterations would, in general, be too much for human fatigue limits.

It is this part of the designer's work that can be done by the digital computer.[20, 21] It is accurate in its calculations, however numerous and complicated. It can carry out, if necessary, a large number of iterations with ease and continue with successive approximations until a result is achieved which meets completely the design and specification requirements. Fig. 2 shows a typical example of computer calculations for a 72 MVA 13/141 kV 3-phase transformer. The effect of the digital computer is thus to speed up the calculations and free skilled and experienced designers of much of the mental drudgery connected with the task. The computer can also be used for incidental work of a laborious nature such as the precise calculation of reactance, eddy-current loss, surge stresses, mechanical forces and thermal characteristics.

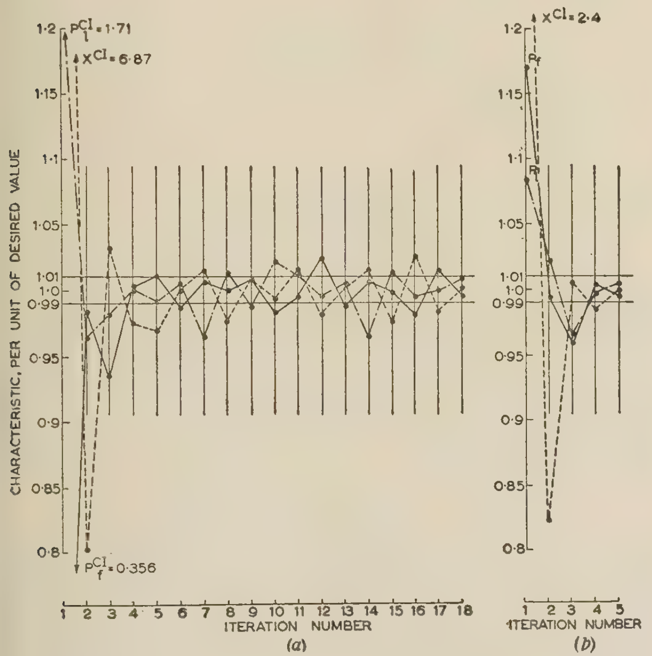

Fig. 2.—Example of computer design by trial and error of a 72 MVA 3-phase 13/141 kV transformer (Fig. 7 of Reference 21).

——— Iron loss.
—·—·— Load loss.
– – – – – Reactance.

(a) Test No. 1.
(b) Test No. 2.

Much work has been necessary in adapting design calculations to computer techniques and in programming the information fed into the computer in a form assimilable by it. The extent to which the computer will take over the whole field of design calculations remains to be seen, but its potential ability to perform the functions just described is already established.

(3) LARGE POWER TRANSFORMERS

There has been a comparatively rapid increase in the size of transformers due to increases in both the voltage and the apparent power rating which, abroad, reached to 400 kV and 600 MVA. In this country, transport restrictions are relatively severe, but 3-phase units of 345 MVA 22/165 kV rating are being built as well as large single-phase units[1] of 330 kV.

The modern practice of superimposing higher-voltage systems on existing networks has increased the use of auto-transformers for interlinkage, especially for 132/275 kV and 275/400 kV combinations. It is now the usual practice to choose 3-phase units wherever transport conditions permit, if necessary using 5-leg 3-phase cores. For the 400 kV range, however, single-phase units become essential, and here the overall height can be reduced by means of 4-leg cores.

When transport limitation is weight rather than dimensions, aluminium-alloy tanks have been used in isolated cases.

Weight reduction, due both to a lighter tank and a saving in oil quantity can be obtained when the tank walls curve both in plan and elevation, the profile following, as far as possible the contour of the windings with the appropriate clearance.[1] Fig. 3 shows a typical example. The design is economical in material, since thinner plates may be used and much less stiffening is required.

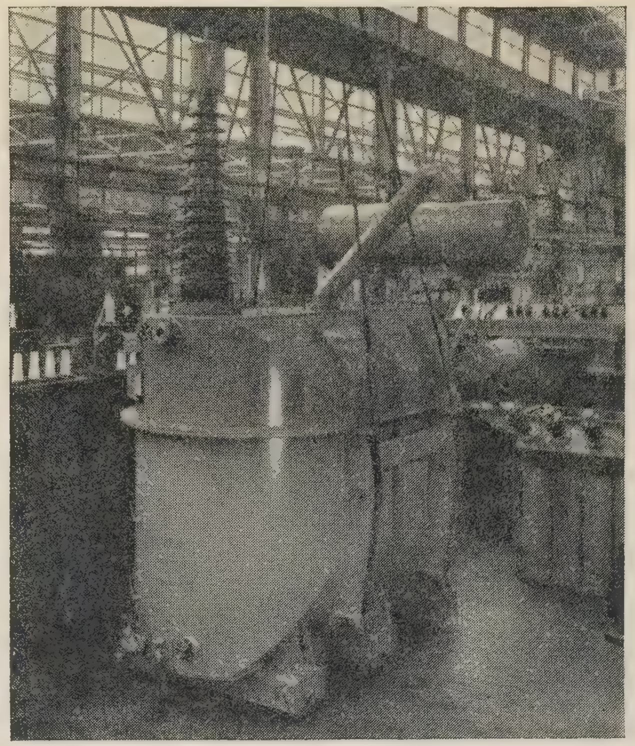

Fig. 3.—24 MVA single-phase 254 kV generator transformer in contour-type tank.

(3.1) High-Voltage Windings

For large power-transformer windings, design is as much a thermal and mechanical problem as it is electrical.

The multi-layer winding described in the last review as 'a recent development' for large transformers has established itself

largely because of its inherent surge strength. However, the conventional disc-coil winding is by no means superseded. Its surge strength has been improved by various methods of electrical shielding and interleaving, and it has its own advantages in mechanical strength and thermal design. Both types of winding are in use—most makers being in a position to use either, depending on their particular assessment of the individual merits of each case.

The use of solid wrapped tubes of thin multi-ply paper as major insulation, also referred to in the last review, has advantages in permitting a compact disc-coil winding arrangement for the highest voltages, especially where a relatively low reactance is desired.

(3.2) Mechanical Strength

The mechanical stresses in power transformers have steadily increased with transformer size and capacity of the supply-system.[22, 24] Short-circuit currents are generally based on the breaking capacity of circuit-breakers, which has increased in 25 years from 1 500 to 25 000 MVA.

The position has been accentuated in recent years by the established practice of auto-reclosing, which makes repeated switching on to possible faults a normal practice, and the introduction of fault throwing for inter-tripping purposes, where short-circuits are deliberately created under normally controlled conditions. The more recent use of line-earthing switches increases the average severity of earth faults without reducing their frequency.

Because of these developments there has been considerable study of the mechanical stresses in transformer windings,[23] including the derivation of formulae for calculating these stresses and the analysis of the resulting performance of the transformer in service.

A major factor in the ability of transformers to withstand short-circuits is the limiting mechanical performance of the materials used in their construction.[25] Emphasis is usually placed on the axial forces which may arise in the winding stacks, due particularly to dissymmetry, and which must be largely resisted by insulating materials. However, for the very large ratings now being considered, the tensile characteristics of the winding conductor to resist the radial forces may well prove the limiting factor.[22]

The cumulative effects of repeated short-circuits are being studied, both with regard to radial stresses and stress/strain relations for stacks of coils, and forecasts of the mechanical life in service of given transformers have been made. It is clear that the mechanical strength of a transformer is not a simple single value as it is generally assumed; some strains are progressive and some of the stresses are cumulative, leading to short- and long-term characteristics.

Suggestions have been made for predetermining the performance in service of a given transformer and its expectation of life in terms of the number of short-circuits for any given service condition.

(3.3) On-Load Tap-Changing Gear

The major development in the period under review has been the pronounced trend towards resistor- instead of reactor-type switching.[26] The relative merits of the two systems were given in the last review. While each type can be designed quite satisfactorily for any given service, the resistor type is more suitable for high-voltage operation and the switching duties are easier. There has consequently been considerable development in the high-speed spring operation of transfer switches, giving arcing times within one half-cycle with correspondingly reduced burning of contacts. Whereas contact lives of the order of tens of thousands were once considered satisfactory, the aim now is more in the region of hundreds of thousands.[27]

One disadvantage of the resistor method is the number of tapping connections required, but these can be reduced in the larger ranges by using reversing switches in combination with half the range of tappings or by separate coarse and fine selector switches.

The growing use of auto-transformers in high-voltage systems, mentioned in Section 3.3, has rendered the usual connection of on-load tap-changing gear at the transformer neutral point relatively inefficient even for directly earthed systems. For an auto-transformer of 2 : 1 ratio, for example, the necessary tapping range would be doubled. An important development during the last few years has been the design of tap-changers insulated for direct connection at the line terminal of the lower-voltage systems.[1] A typical example is the use of tap-changers at the 132 kV line point of the 120 MVA 275/132 kV auto-transformers installed in the British Supergrid system.[42] This is the first time that on-load tap-changing gear has been used directly in such a high-voltage line, and it represents a major step forward. The size of the step will be obvious when it is realized that the whole of the switching gear and tapping connections must withstand the full surge tests with chopped and full waves for the 132 kV system. In some cases a measure of voltage control under surge conditions is obtained by non-linear resistors housed in the selector switch tank and connected across the tappings.

The trend mentioned in the last review towards the use of on-load tap-changing gear on generator transformers has continued, usually with a reduced range of voltage control and fewer tappings than for transmission transformers.

Hitherto, on-load tap-changing gear has developed independently in different countries, and more or less independently among different manufacturers in the same country. In recent years there has been more international effort towards a common practice and eventually some degree of standardization by means of International Study Group Discussions and Conferences at C.I.G.R.É. British engineers have made important contributions to these discussions, which are summarized in Reference 26.

(3.4) Preventive Maintenance

The susceptibility of transformer oil to depreciation from internal or external causes has long been a problem in service maintenance. The general practice in this country is still the well-known conservator and dehydrating breather with some form of Buchholz or gas-detecting relay. Some progress has been made in the practical analysis of evolved gas to assist in determining the origin of breakdown.[28] It is found, for example, that the presence of carbon monoxide in the evolved gas indicates the burning of solid insulation as distinct from that of oil alone.

Efforts to protect the oil from oxidation by nitrogen sealing with automatic pressure regulation have become common in America. It is being realized, however, that changes in temperature and pressure which can occur in service are liable to release dissolved gas in the oil in the form of minute bubbles which can seriously affect the electric strength of a transformer.

A recent nation-wide examination in the United States of a large number of transformers in service, of all ages and selected at random, has shown negligible signs of progressive deterioration of the oil in service even over many years.[29]

(4) DISTRIBUTION TRANSFORMERS

At the beginning of the period of this review the then British Electricity Authority had agreed with transformer manu-

facturers on a standard specification for distribution transformers (B.E. Specification T.1). It covered transformer ratings of 5–1 000 kVA 3-phase and voltages of up to 33 kV. The introduction of this specification had a profound and progressive effect upon the design and construction of distribution transformers in this country and has also contributed important economies to transformers for export.[19] The replacement of a large number of varied designs by one standard for each size has made possible a detailed study of each design and a closer approach to optimum proportions.

This introduction of repetitive manufacture of transformers to standard designs enables the whole production planning to be reorganized to give a shorter manufacturing time cycle at a lower unit cost. Each size of transformer can be planned for batch production, the most economical batch size being determined by factors such as the total number of man-hours per unit, production-line capacity, impregnation and processing cycles, setting-up times, and machine loading. Components and sub-assemblies common to a number of different ratings of transformers can be grouped together and planned for quantity production for stock, with consequent reduction of unit cost.

Repetition of standard batches enables the purchases of raw materials to be in sizes to suit the most economical cutting of components such as tank plates, clamps, tubes and insulation, with subsequent reduction in material wastage.

The use of jigs and templates, for drilling, bending, punching and many other operations, eliminates the lengthy operations of individual marking-out, and ensures greater accuracy in construction.[19]

Inspection is simplified and expensive rectification work in later assembly operations is eliminated. The provision of adequate jigs and tooling can now be justified with long runs of standard units, where formerly it was not feasible.

Similarly, mechanical handling aids, such as mechanized conveyors and pallets, require that the product handled shall be reasonably standardized. Many distribution transformers are being manufactured on roller-conveyor production lines. The creation of stocks of complete standard transformers enables peaks in the manufacturing load to be more easily eliminated and customers' short delivery requirements to be met. Prior to the introduction of B.E. Specification T.1, this manufacture for stock could only be undertaken on a very limited scale, and then only at considerable risk to the manufacturer.

For the smaller transformers of 5–15 kVA single-phase usually known as rural transformers, where iron loss is of great importance, grain-oriented steel is particularly effective, and this is utilized in the most efficient way by some form of wound or clock-spring core.[19] The superior magnetic properties of such cores over the interleaved types result in the iron loss being reduced to 25–30%, the magnetizing current by some 70% and the core weight by about 10%. Fig. 4 shows the core and windings of a single-phase rural transformer with wound core and polyvinylacetal conductor insulation, as mentioned in Section 3.2.

Grain-oriented steel in continuous sheet form is becoming available in this country, and will promote many advances in the methods used in laminated-core production. The installation of gang slitting machines, automatic feed mechanisms, and continuous annealing furnaces will all result in quicker production rates, with reductions in the material and labour required for this operation.

Complete elimination of the very small fire risk associated with the use of transformer oil is sometimes justified in particular installation conditions, and can be achieved either by the use of dry-type transformers (considered in the next Section) or by means of a non-inflammable synthetic liquid of the chlorinated

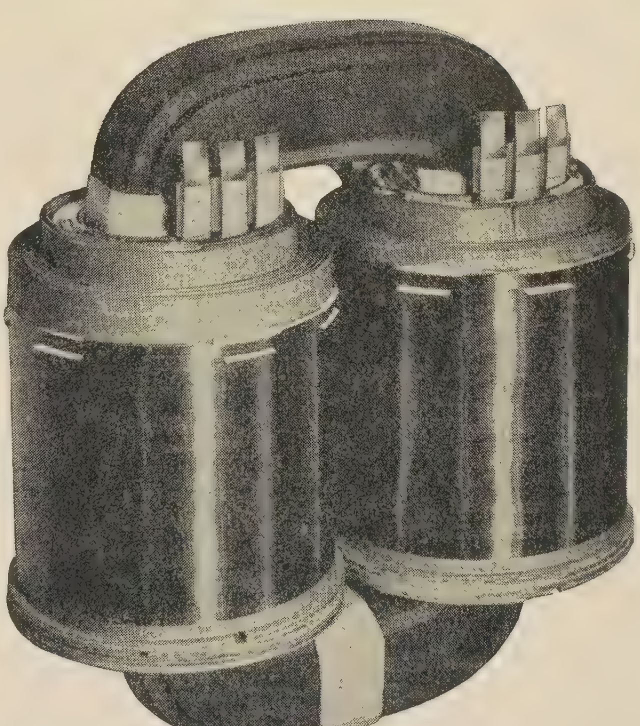

Fig. 4.—Core and windings of a single-phase rural-type transformer with wound core and polyvinyl-acetal insulated conductor.

diphenyl type. Its insulating and thermal properties make it suitable for replacing transformer oil.[19] On the other hand, it is a solvent for many of the resins and plastics normally used in transformer construction. It is much heavier than transformer oil, it has a lower thermal conductivity and a higher evaporation rate. These restrictions can, however, all be taken care of in the design with some increase in cost, which is much accentuated by the relatively high cost of the fluid itself. Its application has thus been restricted hitherto to cases where the fire risk is particularly serious.

(5) DRY-TYPE TRANSFORMERS

The stimulus given to dry-type transformers, by the introduction of new synthetic resins and plastics capable of withstanding much higher temperatures than the usual organic fibres, has induced transformer makers to develop and market new designs using these newer materials, generally in combination with grain-oriented sheet steel.

This development is greatly complicated by the large number of different materials and their combinations now on the market covering a wide range of electrical, thermal and mechanical properties. The initial attempt to include them in a new Class-H insulation with an appropriately higher temperature limit has proved quite inadequate. Additional classes were introduced, still with attempts to associate particular insulating materials with a particular class. Such classifications are standardized in I.E.C. Recommendations 85[31] and in B.S. 2757,[30] with definitions of seven classes.

The practical difficulty of including all the new materials continuously being introduced under different trade names by every manufacturer of insulating materials forced the inclusion in each class definition of the clause 'other materials or combinations of materials may be included in this class if by experience or tests they can be shown to be capable of operation

at the class temperature'. Such a clause is open to wide interpretation affected by innocence, ignorance or optimism, and the situation clearly needs some closer control.

The problem has been receiving consideration in the United States, where dry-type transformers are much more widely used than in Europe.[32] It is recognized that the properties of many insulating materials cannot be determined solely on the basis of their composition. With the greatly increased number and variety of insulation materials that are available, and in view of the very wide range of their properties, it is necessary to determine the performance of each insulating material and its appropriate temperature limits by actual tests. It is also recognized that there are many distinct components in the make-up of the complete insulation system of a particular equipment, each component having a different function to perform and each being subjected to different mechanical and electrical stresses, and often to different temperatures.

Therefore, in the revision of the American I.E.E. Standard No. 1[32] a clear distinction is made between the temperature classification of insulating materials and the assignment of limiting temperatures for complete insulation systems. Test procedures are being developed for this purpose and for different classes of apparatus. A sample or prototype is subjected to a high temperature for a prescribed period, and its condition is checked by applying a suitable voltage. The process is repeated until failure occurs. Vibration, moisture and other auxiliary means may be employed to develop and detect weaknesses in the insulation. For smaller apparatus, full-scale prototypes are tested, whilst, for the larger apparatus, standardized models are specified.[33]

The wider use of dry-type transformers, where cost is a consideration, is restricted in this country largely to applications where the fire risk is so serious that special precautions are justified.

(6) VOLTAGE REGULATORS

On-load tap-changing gear, considered in Section 3.3, is, in general, not normally fitted to distribution transformers for either rural or urban networks. This is partly because on-load tap-changing gear is relatively more expensive for the smaller sizes of transformers and partly because the voltage regulation is more effectively and economically applied somewhere along the feeder or distributor.

Automatic voltage regulators are standardized for this purpose in pole-mounting, feeder-pillar or street-pit construction. They can be quickly installed to correct excessive voltage drops due to growth of load or extension of feeder and thus postpone capital expenditure on the more expensive remedies of additional lines or substations. Even where the voltage regulation is within the prescribed limit, automatic voltage regulators can often be justified by the resulting increase in revenue.

The practice of maintaining a floating stock of these standard regulators has been simplified recently by series-parallel coil connections giving alternative voltage ranges and ratings for each standard size of regulator.

There has been a steady increase, accentuated by the growth of automation, in the automatic control by voltage regulators of industrial processes in many fields—electrochemical, metallurgical, textile, etc. In many cases the regulator takes the material automatically through a complex programme of thermal, mechanical, chemical or electrical treatments. The extent of this work is not generally realized, as applications are usually concerned with modern developments, and they are competitively confidential and so receive little publicity.

In a recent development, the moving-coil regulator, in addition to its normal duty of controlling voltage for regulation or processing work, is provided with means for releasing the moving coil instantly so that the voltage can be reduced to zero in less than a second. Loads can thus be switched on or off without any switches, and even short-circuits can be handled gently and smoothly (again in a fraction of a second) without breaking arcs or power currents. The mechanical forces due to the short-circuit accelerate the clearing operation. The initial application of this construction has been to v.h.f. transmitting equipments.[35] All main and auxiliary contactors and fuses are eliminated in the power-distribution circuits, and no sequential starting-up processes for filaments, water supply, etc., are necessary.

(7) REACTORS

There has been little change in the design and construction of either series or shunt reactors. Further study has been given to the determination of eddy-current and stray losses in both windings and tank due to leakage flux.[36] This is a much more difficult problem in reactors than in transformers, since the so-called leakage flux can be a large proportion of the total.

The growth of very long high-voltage lines has increased the demand for shunt reactors. For large transmission systems they are frequently connected to the tertiary windings of the main auto-transformers, but sometimes it is desirable to locate them directly in the high-voltage lines, and orders for 275 kV reactors have, in fact, recently been placed.

(8) MINING-TYPE TRANSFORMERS

Interest has been shown by the National Coal Board in non-flameproof transformers of up to 500 kVA, as against the 300 kVA previously envisaged. This advance to 500 kVA can be achieved with little increase in the dimensions, primarily owing to the use of grain-oriented sheet steel (Section 2.1). National Coal Board Specification Nos. P.108/1954 and P109/1954 deal with standard and universal non-flameproof mining-type transformers for use below ground.[37, 38]

Mining conditions clearly emphasize the attraction of oil elimination.[39] Dry-type transformers for 100 kVA in flameproof enclosures complying with B.S. 229: 1946 are being manufactured. Such transformers are in use in the United States and on the Continent although the definition of flameproof enclosure is somewhat variable.

Discussions have taken place with the National Coal Board on the use of dry-type transformers at the coal face. Nitrogen filling for the transformers in a non-flameproof tank may be permissible, even though the equipment may be required for use at the coal face. Consideration has been given to the manufacture of transformers using insulation to class H or C of B.S. 2757: 1956.

As stated in Section 5, dry-type transformers are normally appreciably more expensive than oil-immersed types. Nevertheless, the advantages of oil elimination and the possible saving in weight and space due to the use of high-temperature insulating materials such as glass-fibre and silicones are so attractive that, in due course, a dry-type construction may be evolved which will come into general use to the exclusion of the usual oil-immersed type. However, this state has not, as yet, been reached.

(9) RECTIFIER TRANSFORMERS

The main development has been the increase in the size of units required and the addition of phase-shifting arrangements, either on the main windings or as separate auto-transformers mounted in the main tanks. In respect of size, it may be noted that equipments for 12 MW have been supplied.

Much progress has taken place in connection with germanium and silicon rectifiers, and these appear to have a considerable future. Transformers for these rectifiers do not require complex winding connections and are not subject to the heavy mechanical and voltage surges experienced with mercury-arc rectifiers, but these developments are still in the early stages.

The 50 c/s electrification of sections of the British Railways main lines involves practically a new departure in transformer design.[40] Transformers are required for use on both 6.6 and 25 kV 50 c/s supplies.

The main transformers can be divided into two groups—up to 1 MVA for motorcoach units with the equipment mounted on the frame underneath the floor, and from 1 to 4 MVA for locomotives with the equipment mounted above the floor. Both types have to be designed to give minimum weight for their output, and the use of grain-oriented steel at high flux densities has helped to achieve this. High current densities with high flow and forced oil cooling are also employed.

The motorcoach main transformer has to have very low headroom, while other dimensions are limited by the constructional details of the underframe. The dual primary voltage requires that a series-parallel switch be accommodated in the transformer tank. Low-voltage regulating tappings are used with the tap-changer.

For the locomotive, the main transformer dimensions are not so important, but the core and windings are more complicated as the high-voltage method of tapping is normally used. In this method a tapped auto-transformer feeds a constant-ratio double-wound transformer. For the necessary number of control notches, up to 40 tappings may have to be made to the auto-transformer through a high-speed oil-immersed tap-changer. The dual primary voltage is accommodated by a tapping on the auto-transformer which must be capable of handling the full apparent power. Headroom is limited to a certain extent, and the high-voltage bushing has to pass through the roof of the locomotive.

Other equipment, such as smoothing chokes, auxiliary transformers, magnetic amplifiers, etc., are required on both types of stock. These can either be included in the main oil-cooling circuit, or, if mounted separately, can be provided with large quantities of cooling air arising from the train movement.

(10) FURNACE TRANSFORMERS

Up to a few years ago the largest furnace transformers in use in England were of the order of 8 MVA on an 11 kV supply. The secondary-voltage range was obtained by varying the primary winding, the tappings being changed by an off-load switch following the opening of the main circuit-breaker. The wide variation in secondary voltage, of anything up to 75%, required a correspondingly wide variation in flux density, but the actual winding variation was kept to a minimum by using a star/delta change-over.

As the rating increased, it was desirable to use higher-voltage supplies, of 33 or 66 kV, and from the point of view of the furnace to be able to vary the voltage on load. The star/delta change-over was considered impracticable with on-load tap-changing, and the primary-winding tapping range would therefore have had to correspond to the secondary-voltage range. Such an arrangement, particularly with the higher supply voltages, can result in serious over-voltages on the tap-changer, and in view of this and the increasing load current, a separate regulating unit may be used. This may be of the auto- or double-wound type, and it has a tapped secondary winding corresponding to the voltage range required on the primary winding of the main furnace transformer.

The on-load tap-changing gear relieves the main circuit-breakers of the very frequent operation (upwards of 100 000 times a year) hitherto required by off-load tap-changing, and reduces it to an annual figure of 5 000–6 000, which is within the capabilities of large air-blast switchgear.

Recent installations include a 20 MVA 3-phase arc-furnace transformer, operating directly from a 66 kV supply.

(11) HIGH-VOLTAGE TESTING TRANSFORMERS

An essential requirement of a high-voltage testing equipment is a sinusoidal high-voltage waveform, especially for cable testing where dielectric loss measurements are important.

With the usual capacitive loads, there is always a possibility of resonance between the load capacitance and transformer reactance. If this should happen, as it well may, at one of the supply-frequency harmonics, the waveform of the terminal voltage will be distorted. A recent development[41] which is rapidly extending is to substitute for the conventional transformer a series circuit having a variable reactor tuned to resonate with the test capacitance at 50 c/s. Since the circuit is now deliberately in series resonance at the fundamental frequency it cannot resonate at any of the harmonics, and this type of waveform distortion is eliminated.

In the larger higher-voltage testing equipments where transformers are connected in cascade, the voltages across the transformers are not equal, owing to uneven distribution of the circuit impedance. In the series-resonant circuit this division depends on the impedances of all transformer-reactor units, which are identical and under control.

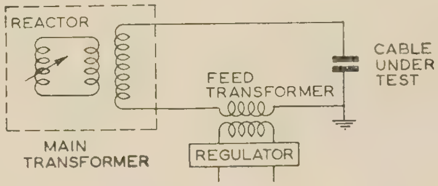

Fig. 5.—Diagram of series-resonant circuit for high-voltage testing transformers.

The circuit is shown diagrammatically in Fig. 5. A continuously-variable reactor of the moving-coil type is used to tune with the load capacitance at the fundamental frequency. The voltage regulator for varying the voltage requires sufficient rating to supply the losses of the circuit only.

The only limitation experienced with this circuit is that there is a lower limit to the test capacitance, since a reactor of infinite reactance would be required to tune with a zero-capacitance load. In most cases this limit is below the minimum capacitance of any test specimen, but otherwise a suitable dummy load must be connected in parallel with it.

Progress in the design of high-voltage transformer windings and bushings has made possible the outdoor installation of the transformer with considerable saving in building capacity owing to the large clearances that would be necessary if the transformer were installed inside. Fig. 6 shows a single-unit 1 MV transformer recently supplied to the National Physical Laboratory, together with a 1 MV double-ended wall-condenser bushing enabling the full voltage to be brought into the Laboratory.

(12) ACKNOWLEDGMENTS

Acknowledgments and thanks are due to the National Coal Board, the Central Electricity Generating Board, the Electrical Research Association, the National Physical Laboratory and to

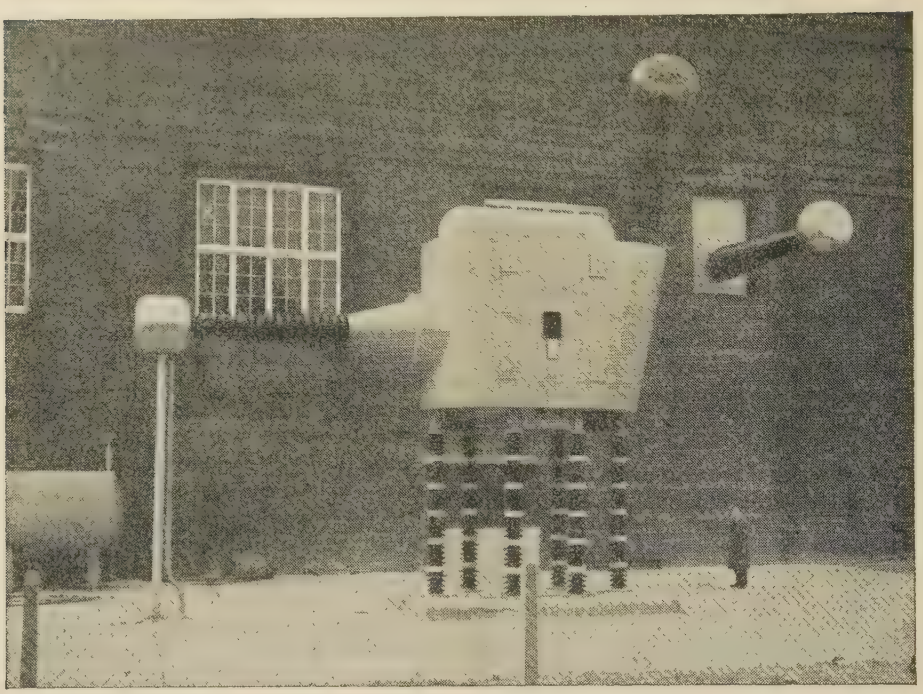

Fig. 6.—Single-unit 1 MV 50 c/s outdoor testing transformer with double-ended wall bushing.

the following transformer manufacturers for information and assistance in the preparation of the review: Brush Electrical Engineering Co., Ltd., English Electric Co., Ltd., Ferranti, Ltd., Fuller Electrical and Manufacturing Co., Ltd., General Electric Co., Ltd., Hackbridge and Hewittic Electric Co., Ltd., Metropolitan-Vickers Electrical Co., Ltd., and C. A. Parsons and Co., Ltd.

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THE BREAKDOWN OF TRANSFORMER OIL UNDER IMPULSE VOLTAGES

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(*The paper was first received* 29*th March, and in revised form* 21*st June*, 1957. *It was published in September*, 1957, *and was read before the* North-Western Measurement and Control Group 18*th February, and before a joint meeting of the* Measurement and Control Section *and the* Supply Section 7*th January*, 1958.)

SUMMARY

The breakdown of pretreated transformer oil has been studied under controlled conditions with microsecond-duration impulse voltages. The oil was filtered, degassed and dried in a circulatory oil-cleaning system, and 3-gallon samples were tested in a glass test-cell with voltages of up to 500 kV. The strength was found to depend, not only on the duration of the voltage, but also upon the manner in which it was applied. Several experiments are reported in which oil of a consistent quality was tested to study the effect of such factors as the size, material and preparation of the electrodes. In all these tests the strength was found to vary as the test proceeded—an effect known as 'conditioning'.

It is shown that one of the main factors which determines the breakdown strength obtained is the presence of microscopic gas bubbles on the surface of the electrodes, and these, together with solid particles and fibres in the oil, may well account for the low strengths obtained by previous workers who used undegassed or unfiltered oil.

(1) INTRODUCTION

Oil of vegetable or mineral origin has been used for electrical insulation since the beginning of the century, and it is therefore natural that the causes of breakdown in liquid dielectrics should have been the subject of much research. In recent years there has been a tendency towards the testing of small samples of carefully purified oil, and this has been accompanied by a decrease in the scale of the apparatus employed and the use of lower test voltages. The work reported in the paper was undertaken to study the impulse breakdown of larger samples of pretreated oil with higher voltages, and thus to find the increase in strength which might be obtained in practice by pretreatment of the oil and to show the relevance of small-scale tests to industrial requirements. The results given constitute the preliminary measurements in the investigation, and are so presented as to emphasize the dependence of the measured breakdown strength on the test procedure adopted and the electrodes used in the tests—points which are not always sufficiently appreciated. For example, variations in the recorded uniform field strength of as much as 30% may result from the manner of application of the voltage, and a change of 50% may be brought about by the method of preparation of the electrodes.

(2) APPARATUS

(2.1) The Test Cell

All previous work on the breakdown of transformer oil at voltages in excess of 200 kV has been carried out in open test cells,[1–3] usually consisting of an insulating container with a high-voltage bushing partially immersed in the oil to support the upper electrode. With such an arrangement the oil is open to contamination from dust and moisture in the atmosphere. Furthermore, the test gap has rarely been under observation during the test, since opaque containers have been generally employed. One of the main problems in the design of a cell is to keep the volume of oil required for each test as small as possible. Goodlet, Edwards and Perry,[1] working with voltages of up to 900 kV, used an open cell with a capacity of 270 gal. In the present work, by careful design, it has been possible to construct a totally enclosed cell capable of withstanding 600 kV, yet requiring only 3 gal of test liquid. The working voltage could probably be further increased to 800 kV by the use of suitable stress distributors. The test cell, which is shown in Fig. 1, was constructed from three sections of standard Pyrex

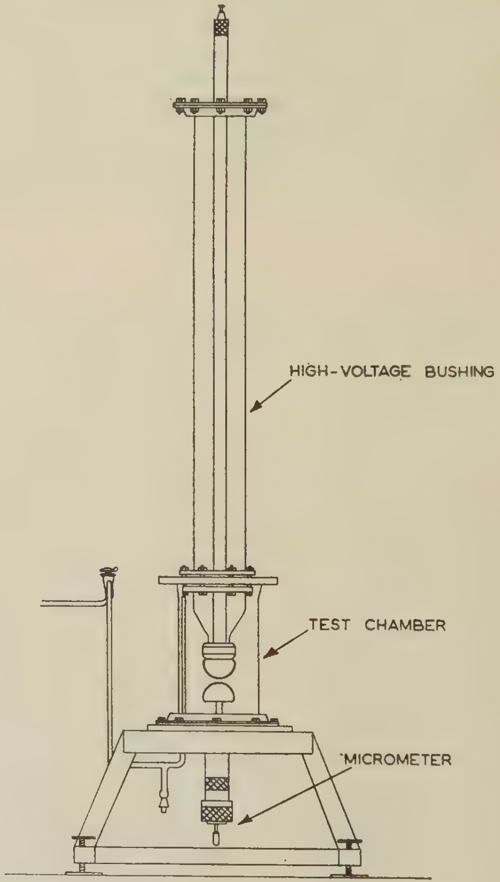

Fig. 1.—Test cell (1/24th full size).
For clarity, only one of the connections to the oil-cleaning system is shown.

glass pipe-line. The test chamber itself was 9 in in diameter and 18 in high, and was surmounted by a high-voltage bushing which was permanently filled with oil similar to that used in the tests. This bushing consisted essentially of a 5 ft length of 6 in diameter Pyrex pipe, with a concentric brass conductor of $1\frac{1}{2}$ in diameter, terminated at the lower end by means of a tapered glass section. The top and bottom of the test chamber consisted of Perspex discs, and the cell was made vacuum-tight by means of Neoprene gaskets. The oil was admitted to, and taken from, the cell by means of four glass tubes passing through vacuum

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seals in the base of the test chamber, which was completely filled with oil during the tests.

In all the tests reported in the paper (with the one exception noted in Section 4.2) the electrodes took the form of hemispheres or hemispherically ended rods, thus giving an approximately uniform field for small gap settings. High-voltage electrodes up to 3 in in diameter could be accommodated in the cell by screwing them to the bottom of the high-voltage bushing. An earthed electrode of up to 6 in diameter could be mounted on an adjustable support which passed through a sliding vacuum seal in the base of the test chamber and was connected to a micrometer. Gaps of up to 6 in could be obtained, and could be measured with an error of less than $\pm 0{\cdot}5\%$ by means of ground spacers used in conjunction with the micrometer.

(2.2) The Oil-Cleaning System

The test cell was connected to a circulatory oil-cleaning system in which solid particles and fibres, together with a large proportion of the dissolved gas and moisture, in the oil could be removed. This was accomplished by passing hot oil through a sintered-glass filter of 20–30 microns pore diameter (No. 3 porosity) into a vacuum. The method is similar to that used by Watson and Higham.[4] The oil could be filtered as many times as required before being admitted to the test cell, each cycle taking between 4 and 6 hours when the oil was heated to 70° C.

The cleaning system was of all-glass construction except for some sections of the vacuum line, and is shown in Fig. 2. For

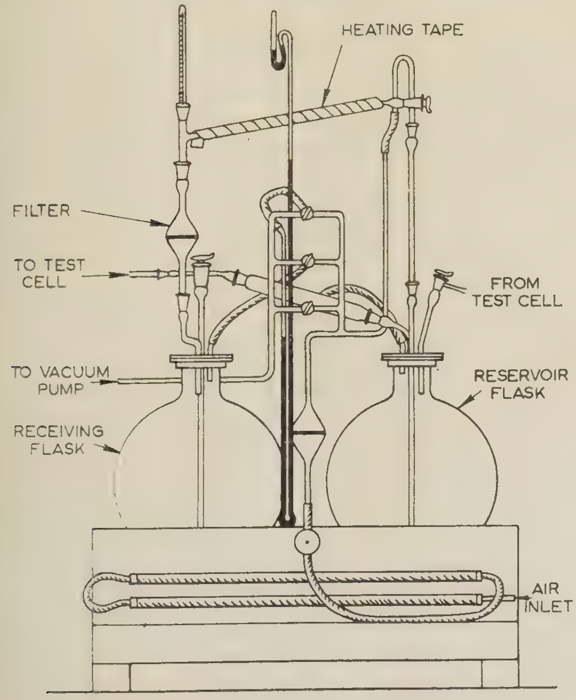

Fig. 2.—Oil-cleaning system (1/16th full size).

simplicity, the glassware was connected together by means of standard cone-and-socket joints, but difficulty was experienced in keeping these vacuum tight, since any sealing grease was slowly washed away by the combined action of the vacuum and warm oil. Finally, a technique was devised using ungreased joints over which a polythene sleeve was welded. This was found to give a good vacuum joint which could easily be broken when necessary. A vacuum of between 0·1 and 0·01 mm Hg could be maintained by means of a high-capacity rotary pump, and the system was normally kept under vacuum at all times except during a filtering cycle or a test. When it was required to keep the system under atmospheric pressure for any length of time, nitrogen was admitted instead of air, to reduce oxidation of the oil. In either case, any gas admitted to the system was dried by passing it through calcium-chloride and phosphorus-pentoxide tubes and then through a sintered-glass filter of 5–10 microns pore diameter (No. 4 porosity).

(2.3) The High-Voltage Circuit

The impulse voltage was derived from a 1 000 kV 6-stage Marx–Goodlet impulse generator supplied from a 180 kV d.c. Cockcroft doubling circuit.[5] This generator had the advantage of a low output capacitance, thus limiting the energy dissipated in the discharge when breakdown occurred. In order to limit further the damage to both the oil and the electrodes when breakdown occurred, no additional load capacitance was included in the circuit and the highest possible wavefront resistance was used. Under these conditions the impulse generator gave an approximately 2/60 microsec voltage waveform, but subsidiary measurements have shown that the breakdown strengths obtained under these conditions are not likely to be more than 1% below the values that would have been obtained with a standard 1/50 microsec voltage waveform.

In the course of the investigation a 3-stage high-voltage surge diverter incorporating a trigatron became available,[6] with which it was possible to short-circuit the impulse generator within a few microseconds of breakdown occurring. This further reduced the damage to the oil, but had no effect on the breakdown strength, and so was seldom used since it made the operation of the equipment more difficult. The circuit arrangement and component values are given in Fig. 3.

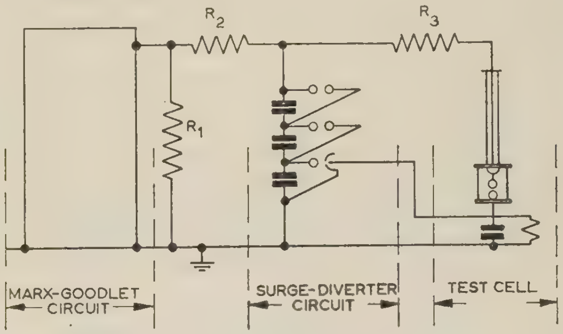

Fig. 3.—High-voltage circuit.

R_1, wave-tail resistor, 33 kΩ.
R_2, short-circuit resistor, 660 Ω.
R_3, wavefront resistor, 20 kΩ.
Impulse-generator capacitance per stage, 0·018 μF.
Surge-diverter capacitance per stage, 500 pF.
Test-cell capacitance, 65 pF.

(2.4) Voltage and Time-Lag Measurements

Because of the position of the wavetail resistor in the high-voltage circuit, it was impossible to use this as a voltage divider for measurements in the normal manner. It was therefore decided to calibrate the impulse generator by means of an impulse peak voltmeter used in conjunction with a high-impedance capacitive voltage-divider across the test cell. The voltage waveform was recorded independently between breakdown tests by using the test cell itself as the high-voltage capacitor of a voltage divider.

The circuit for the peak voltmeter is shown in Fig. 4, and operates as follows: The capacitors C_1 and C_2 form a high-

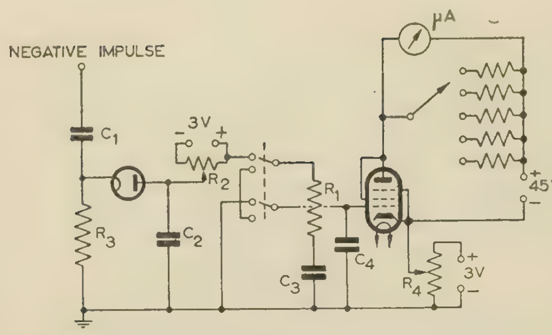

Fig. 4.—Peak voltmeter circuit.

R_1, 16 MΩ.
R_3, 10 000 MΩ.
C_2, 5 pF.
C_3, 0·001 5 μF.
C_4, 0·02 μF.

voltage potential divider during the front of the voltage waveform. After the peak of the impulse has passed, the charge collected on C_2, which is proportional to the peak voltage, is divided between C_2 and C_3, and between C_3 and C_4 after the operation of the switch. This reduces the voltage to a level suitable for measurement by means of the electrometer pentode. The resistor R_3 holds the cathode of the diode at earth potential between impulses, and the battery and resistor R_2 balance the thermionic current from the diode keeping C_3 uncharged before a measurement. The auxiliary circuit may be designed to allow the measurement of the peak value of a single impulse or of a series of increasing impulses. The instrument was calibrated by replacing the test cell with a standard high-voltage capacitive voltage divider of approximately the same capacitance and using a cathode-ray oscillograph. In this way an error of less than $\pm 2\%$ in the voltage measurement was obtained. Since the construction of this voltmeter, a similar instrument has been described by Schwetzke.[7]

Automatic photographic recording of the time lags before breakdown was achieved by means of a cathode-ray oscillograph and pick-up aerial. The time-base of the oscillograph was tripped in synchronism with the impulse generator, and subsequent breakdowns in the test cell were observed as a high-frequency oscillation on an otherwise straight trace. By this means, 25 impulses per inch of film were recorded, the camera being controlled by means of thyratron-controlled relays.

(3) EXPERIMENTAL PROCEDURE

(3.1) Preparation

Before each series of breakdowns, both the oil and the electrodes were carefully prepared. When new oil was first admitted to the system it was subjected to several filtering and degassing cycles before the first test. The oil was refiltered twice between successive series of breakdowns, and by doing this it was possible to use the same sample of oil for up to six months without any deterioration in strength.

The electrodes were polished on a high-speed buffing mop. After the craters from the previous test had been removed with emery cloth, the electrodes were rough buffed on a hard mop, care being taken to retain their shape. This was followed by one or two final polishings with softer mops and suitable polishing compositions. After degreasing in hexane or acetone and an examination under a metallurgical microscope, the electrodes were placed in the test cell, which was then evacuated. Prior to each test the cell was flushed through with clean oil before being filled in preparation for the first breakdown.

(3.2) Test Procedure

A feature of previous investigations, especially those using high voltages, has been the large scatter in results during each test, coefficients of variation of between 5% and 20% having been obtained. In such experiments, large numbers of breakdowns are required in order to obtain a mean strength with any accuracy. In the present work it was considered more profitable to spend time and care in the preparation of the oil and the electrodes and also in performing the tests in order to reduce this scatter. As a result of this, it has been possible to reduce the coefficient of variation in many of the tests to below 5%, which represents a marked improvement. Throughout the investigation great attention has been paid to the reliability of the results, and all the tests reported in the paper have been repeated several times. In some cases the tests were repeated with new samples of oil after some 12 months and the same results were obtained.

The testing technique which has been employed by the majority of previous workers, and which is therefore referred to here as the normal technique, consists of the application of a series of impulses of increasing magnitude until breakdown occurs. The breakdown strength is then calculated from the peak value of the impulse which caused breakdown. The rate at which the impulses were applied was varied but was found to have no effect. In the present investigation the impulses were applied at the rate of 30 per minute, increasing in peak value from 40% of the expected breakdown voltage in steps of 1%. This method is simple and gives a value of the breakdown strength for each breakdown, but it is valid only if the application of successive impulses before breakdown does not affect the breakdown strength. There is no evidence to suggest that the insulating properties of the oil are adversely affected in these circumstances, but rather that the strength is raised.[8] This led to an alternative test procedure.

A different method of measuring the minimum voltage required to break down an insulating gap is that adopted by Sorenson.[3] An impulse of peak value greater than that required for breakdown is applied to the test cell, and this is followed at regular intervals by a series of decreasing impulses until breakdown ceases to occur. The breakdown strength is then determined from the last impulse to cause breakdown. This procedure is usually adopted by investigators who are interested in the variation with over-voltage of the time lag before breakdown, and appears to yield a lower breakdown strength than the normal technique. In order to compare these two techniques, a composite procedure was adopted for some of the tests reported here, which will be referred to as the first impulse technique. This consisted of a number of breakdown measurements similar to the normal testing technique, except that the value of the first impulse of each successive series was progressively increased throughout the test, with the result that a point was reached when breakdown occurred on the application of this first impulse. The strength so obtained corresponds to the minimum breakdown voltage as obtained by Sorenson and may be compared with the results obtained during the same test by means of the normal testing technique.

(4) EXPERIMENTAL RESULTS

All the results quoted were obtained with oil of a constant quality and pretreatment, being commercial oil complying with the B.S. 148:51 and of 28 centistokes viscosity at 21° C. All the tests were carried out at atmospheric pressure and a temperature of 20° C, nominal 2/60 microsec impulses being used except where otherwise stated.

Each breakdown resulted in damage to the electrodes and decomposition of some oil. The latter was minimized by the use of a comparatively large series resistance, but even so the

small cloud of carbon particles which was found in the gap after each breakdown had to be cleared away by circulating the oil in the test cell before commencing the next measurement. The carbon particles were too fine to be removed by a No. 4 porosity filter, but did not affect the breakdown strength when distributed in the bulk of the oil.

(4.1) The Conditioning Effect

Several workers have reported a conditioning effect during breakdown measurements, i.e. an increase in strength with successive breakdowns, but have considered it incidental to the main problem. The effect was observed in the present tests with the normal testing technique and is reported here more fully since it is believed that it provides a clue to the mechanism of breakdown.

The results from four similar series of breakdown measurements using the normal testing technique, in which breakdown measurements were made at 30 min intervals with 6·25 cm diameter brass electrodes, are shown in Fig. 5. The electrodes

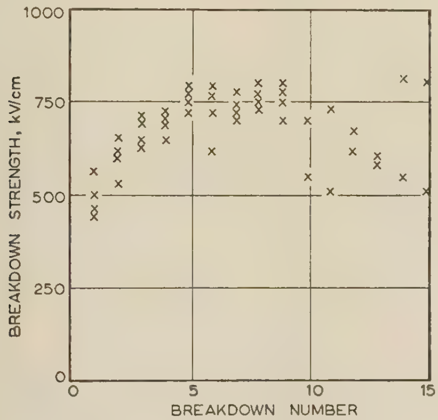

Fig. 5.—The conditioning effect.
Brass electrodes, 6·25 cm diameter, 2·5 mm gap.

were repolished before each series. It is seen that the first breakdown with new electrodes yielded a mean strength of 480 kV/cm and that there was an increase in strength to 750 kV/cm during the first five breakdowns. After conditioning, the strength was steady to within $\pm 3\frac{1}{2}\%$ for a further five breakdowns (with the exception of one, which is discussed below), after which the results became erratic, the mean strength falling to 640 kV/cm and the coefficient of variation increasing to 17%.

During the conditioning period and when a steady strength was obtained, each breakdown resulted in a single isolated pit on each electrode of about 0·1 mm diameter and within 20% of the electrode radius from the point of maximum field. When the results became erratic, however, multiple craters were formed on the cathode, i.e. the pit formed by one breakdown was almost coincident with one formed by a previous discharge. The probability of this arising from random positioning of the cathode pits was negligible, and therefore suggests that, under these conditions, breakdown was initiated at those points on the cathode where pits had already been formed. Part of a cathode is shown in Fig. 6, magnified 72 times, and one single and two multiple pits can be seen.

During the course of a large number of similar tests it was noted that occasionally a breakdown occurred at a voltage considerably lower than would have been expected. One such breakdown is seen in Fig. 5. These were found to be due to fibres which had entered the test cell when the electrodes were

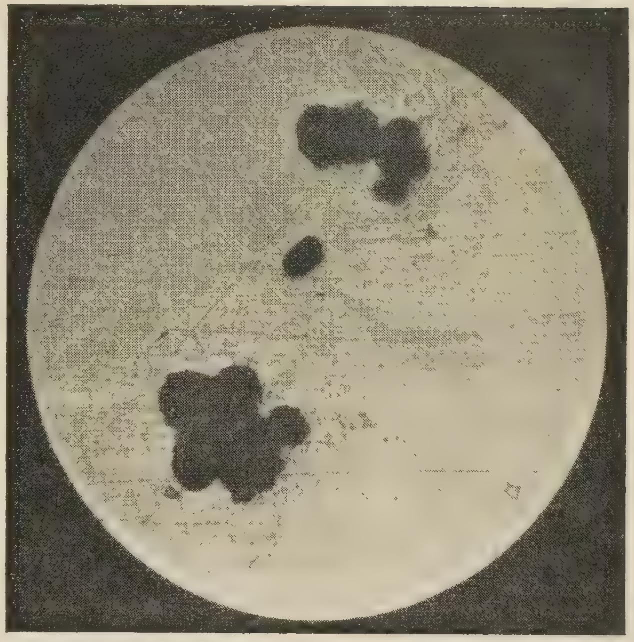

Fig. 6.—Part of cathode showing a single and two multiple pits (magnification of 72).

replaced and which had not been removed by flushing the cell with oil before the test. This was confirmed by examination of the test gap immediately after breakdown, when it was observed that the cloud of carbon particles formed by the discharge was not near the point of the maximum field, as was more usually the case. Examination of the electrodes after removal from the test cell also showed one pair of pits at the side of the electrodes and separate from the others. Such breakdowns yielded a strength 20–40% below the mean steady value and were considered as spurious and hence neglected in calculating the mean breakdown strength.

In one series of measurements testing was temporarily discontinued for 16 hours after eight breakdowns. When the series was resumed the strength was found to have fallen to approximately the same value as for the first breakdown of a similar series. It was also found that if the test cell was not completely filled, thus allowing the oil to absorb gas, conditioning did not occur.

Several series of measurements were carried out under the conditions already described but with the breakdowns occurring at 5 min intervals. In these cases it was found that up to 40 breakdowns could occur before multiple craters began to appear. The mean strength after conditioning was 890 kV/cm, but the scatter in results was greater, the coefficient of variation being about 14%. This scatter corresponds more closely to that found by previous workers with high voltages, but the mean strength is considerably higher than has been previously quoted for electrodes of comparable size.

(4.2) Dependence on Testing Technique

In the tests reported so far the normal testing technique was used, but a number of tests have also been made with the first impulse technique, using both uniform and non-uniform field configurations. With this technique it is impossible to observe conditioning, since several breakdowns occur before a measurement is obtained, and therefore only the steady strength may be quoted. In order to obtain as many results as possible from each pair of electrodes, the measurements were made at 5 min intervals, the other test conditions being as before. The results

for the uniform field test, with 6·25 cm diameter brass electrodes and a 2·5 mm gap, are summarized in Table 1, where it is seen that the mean strength obtained for breakdown on the first impulse was 680 kV/cm, which is 24% lower than the corresponding strength obtained with the normal testing technique.

Table 1

Dependence of Electric Strength on Testing Technique

	Normal testing technique	First impulse technique
Mean breakdown strength, kV/cm ..	890	680
Maximum breakdown strength, kV/cm	1 090	710
Minimum breakdown strength, kV/cm	600	660
Coefficient of variation, %	14	3

A test was performed with a non-uniform field configuration, using a 0·5 cm diameter hemispherically ended steel rod as cathode and a 15 cm diameter steel plate at a gap setting of 2·75 cm. The breakdown voltage for the first impulse technique was 260 kV ± 2%, compared with 380 kV ± 6·5% for the normal testing technique—a difference of 32% of the higher value.

A further series of breakdowns with uniform fields was performed to observe the effect of gap setting on the breakdown strength. Measurements were at 5 min intervals and the results

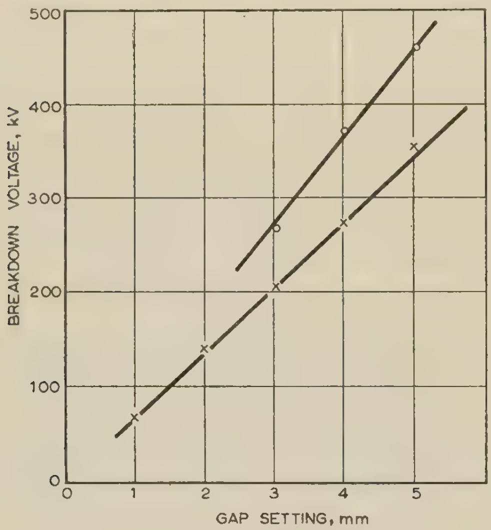

Fig. 7.—Variation of breakdown voltage with gap setting.
Brass electrodes, 6·25 cm diameter.
○ ○ Normal testing technique.
× × Single impulse technique.

are shown in Fig. 7. The difference in strength obtained by the two testing techniques is obvious, but in neither case is there any great dependence of strength on gap setting.

(4.3) Dependence on Impulse Duration

The preceding tests were made with an impulse voltage which approximated to the standard 1/50 microsec waveform, and in order to demonstrate the time dependence of breakdown, a few tests were made with 2/600 microsec impulses. Series of measurements obtained at 30 min intervals, with 6·25 cm diameter brass electrodes and 2·5 mm gap, showed the same characteristic variations in strength as was previously noted. The strength conditioned from 520 kV/cm to a steady value of 680 kV/cm, which is 10% below the previously recorded value for 2/60 microsec impulses. Erratic results accompanied by the formation of multiple craters were observed at the end of each test.

Time-lag records taken during tests with the 2/60 microsec impulses showed that breakdown occurred on or just after the peak of the impulse waveform, e.g. while the voltage was within 5% of its peak value.

(4.4) Dependence on Electrode Area

Several tests were made with 2·5 cm diameter brass electrodes at a gap setting of 2·0 mm. The normal testing technique was used, with breakdown measurements at 30 min intervals. Conditioning occurred more rapidly than with 6·25 cm diameter electrodes, often being complete after only one or two breakdowns. Comparison of the results of several series of breakdowns showed that two or three consistent measurements were always obtained after conditioning and before the occurrence of erratic results accompanied by multiple-crater formation. The mean strength for the first breakdown in each test was 700 kV/cm, and the mean steady strength after conditioning was 990 kV/cm—35% higher than that obtained for 6·25 cm diameter electrodes.

(4.5) Dependence on Electrode Material

Several investigators have performed breakdown measurements with a wide range of electrode materials. In the present investigation the metals which could be used were limited, since the extent of the damage to the electrodes caused by the discharges precluded the use of plated electrodes. Thus the materials used were brass, stainless steel and aluminium. In each case the electrodes had a diameter of 2·5 cm with a gap of 2·0 mm and the test conditions were the same as before.

The results of these tests are summarized in Table 2. The

Table 2

Dependence of Electric Strength on Electrode Material

Electrode material	Steady strength		Mean erratic strength	
	Average	Variation	Average	Variation
	kV/cm	%	kV/cm	%
Brass	990	4	940	12
Stainless steel ..	1 020	3	960	9
Aluminium ..	1 130	2	860	7

tests with stainless-steel electrodes followed the same pattern as those with brass electrodes, a few consistent results being obtained after conditioning before multiple-crater formation occurred. With aluminium electrodes, however, low and erratic results were obtained immediately after the first one or two breakdowns, with an occasional high breakdown value in the course of the tests. These high values were fairly consistent throughout a number of tests and appear to correspond to the consistent results obtained after conditioning with brass and stainless-steel electrodes. This is confirmed by the fact that the erratic breakdowns with aluminium electrodes resulted in multiple-crater formation on the cathode whereas the occasional high values caused single pits. By the use of an anode and cathode of different metals it could be shown that, for a particular sample of oil, the breakdown strength was dependent only upon the cathode used.

(4.6) Dependence on Electrode Pre-Treatment

It was stated in Section 3.1 that the electrodes were kept in the test cell under vacuum before the oil was admitted for testing

Normally the pressure in the cell during this period was only about 20 mm Hg absolute, but in some tests it was reduced to 0·1 mm Hg or less. When this was done the strength determined from the first breakdown measurement of each series was much higher than usual and the conditioning effect was reversed. Both the steady and erratic strengths obtained subsequently were the same as before. The results for the first breakdown of a series with each of the three cathode materials under the two different conditions are given in Table 3. The electrodes were

Table 3

DEPENDENCE OF ELECTRIC STRENGTH FOR FIRST BREAKDOWN ON ELECTRODE PRE-TREATMENT

Cathode material	Electric strength	
	Storage pressure 20 mm Hg	Storage pressure 0·1 mm Hg
	kV/cm	kV/cm
Brass	700	1 130
Stainless steel.. ..	700	1 100
Aluminium	720	1 310

kept at the pressure indicated for 18 hours before the test cell was filled, and the measurement commenced. Further tests showed that the strength obtained before conditioning was independent of the pressure at which the electrodes were stored provided that this was less than 5 mm Hg absolute, but that for higher pressures the strength fell.

(5) DISCUSSION OF RESULTS

From the experimental results one may draw a number of interesting conclusions which are given below. They include the effect of the testing technique on the recorded breakdown strength and the dependence of this strength on the electrodes used for the tests. The results will be compared with those of previous investigations.

Certain observations have led to a further consideration of the theory of breakdown, and this will be dealt with in Section 6.

The tests reported in Section 4.2 on the dependence of the electric strength on the testing technique have shown, in agreement with the results of Harrison,[8] that the strength is increased when a series of increasing impulses is applied. This effect was found to be more pronounced for a non-uniform field with the point electrode negative, suggesting that it is associated with some change in the bulk of the liquid, since little electrode dependence would be expected in this case.

At present it appears impossible to offer any explanation for the results obtained with the first impulse technique. However, they may be important from a practical point of view, since a reduction in strength of nearly 25% was obtained.

Using the normal testing technique a variation in the strength with successive breakdowns was observed. Such a conditioning effect at the beginning of a test has been reported for direct and impulse voltages by several workers, most of whom considered this effect as incidental and quoted only values of the strength obtained after conditioning had taken place. The strength for the first breakdown may be very important in practice, however, and so long as the mechanism of this effect is not properly understood, there is no reason to quote one result rather than the other. It was therefore considered important to study the conditioning effect more carefully in this investigation.

Watson and Higham[4] suggested that conditioning was due to removal by the discharge of small fibres suspended in the oil near the test gap. This explanation does not hold in the present investigation, since the oil was circulated in the test cell after each breakdown. For the same reasons conditioning must be associated with the electrodes rather than with the liquid. It was also noted that conditioning was not permanent and did not occur when the oil contained dissolved gas. It would therefore appear probable that it is due to some form of gas layer on the electrodes. This explanation has previously been advanced by Tropper and Maksiejewski[9] and is consistent with the fact that all investigators who have reported a conditioning effect used degassed liquids in closed test cells.

It was shown in Section 4.1 that the mean strength after conditioning was a function of the time between successive breakdowns. Other workers have found that the scatter in the results tends to increase when the time between the breakdowns is reduced, although none have reported a simultaneous increase in the mean strength. It will be shown later that such an increase can easily be explained, but it is mentioned here to emphasize the importance of specifying all the test conditions when quoting results. The present investigation has shown that the measured strength for a given sample of transformer oil is dependent on several apparently unimportant factors, and it is therefore not surprising to find a wide variation in the results of different workers.

Many of the results have shown how the breakdown strength was affected by the electrodes used in the tests. This has been found by other investigators in the past, and the present work confirms that, not only are the shape and size of the electrodes important, but also their composition. This suggests that electron emission occurs from the electrodes, or more specifically from the cathode, which initiates the breakdown process. It is significant that such an electrode dependence has been reported only when the oil tested is comparatively free from solid particles or fibres which would otherwise initiate breakdown at a lower field strength. It therefore appears that the impulse breakdown of carefully filtered transformer oil is similar to that of pure liquids, being dependent on the emission of electrons from one electrode and their subsequent multiplication in the bulk of the liquid. Tests by Crowe, Bragg and Sharbough[10] confirm this.

When 2·5 cm instead of 6·25 cm diameter electrodes were used, a considerable increase in strength was noticed. It seems probable that, had it been feasible to reduce the electrode size further, the strengths which resulted would be comparable to those obtained by workers who used small test samples and low test voltages. For example, the strengths corresponding to 6·25 cm and 2·5 cm diameter electrodes were 750 kV/cm and 990 kV/cm, whereas Zein El-dine and Tropper,[11] using 13 mm electrodes and 1/50 microsec impulses, reported the strength of treated oil to be 1 450 kV/cm. This shows that, in practice, it is impossible to obtain such high strengths in full-scale high-voltage equipment as have been reported for small-scale tests. Nevertheless, such small-scale tests indicate which factors are of importance if the highest possible strength is required in a practical case.

No dependence on gap setting was observed, in agreement with previous work in which comparable gap lengths were used.[12] In view of this, it is seen that the reported dependence on electrode area is a true area-dependence rather than a dependence on the volume of oil under stress. This is consistent with the proposed breakdown theory in which the breakdown is initiated by electron emission from the cathode.

The few low electric strengths measured in a number of the tests, as reported in Section 4.1, confirm that fibres present in the oil lower its strength. The strength was lowered by between 20 and 40%, and it is possible that the reduction would have been still greater if unfiltered oil had been used. Tests with

6·25 cm diameter brass electrodes gave a strength before conditioning of 480 kV/cm, whereas previous workers with similar electrodes but untreated oil obtained only about 320 kV/cm. The presence of very fine carbon particles in suspension resulting from the decomposition of the oil did not seem to affect the results,[13] and nothing in the results suggests a critical dependence of the strength on moisture present in the oil. This effect, however, was not specifically studied.

The mean strength after conditioning was found to depend on the gas dissolved in the oil: degassed oil exhibited a conditioning effect, whereas undegassed oil did not. It has already been suggested that conditioning is associated with a gas layer on the electrodes, and it is probable that the dependence of the breakdown on gas in the liquid is due only to the effect of this dissolved gas on the surface condition of the electrodes. When the test cell is not well evacuated before being filled with oil, there is an increase in strength with successive breakdowns, owing to the gradual removal of gas from the electrode surface by the discharges. After a few breakdowns a steady strength is obtained which, however, is still below the value which may be obtained for the first breakdown when the test cell is well evacuated before being filled. Moreover, with good evacuation before the oil is admitted, the initial high strength is followed by a decline to the same steady value as in the previous case. Thus, after a few discharges, an equilibrium is obtained which is independent of the initial state of the gas layer on the electrodes, but dependent on the gas content of the oil. This suggests that gas may not only be removed from the surface of the electrodes by the discharges, but may also diffuse to the surface from the bulk of the liquid until a dynamic equilibrium is attained when steady results are obtained. If the rate of diffusion of gas to the surface is constant, this equilibrium will be dependent on the time between the discharges, and this is reflected in the increase in strength when the time between breakdowns was reduced from 30 to 5 min, as reported in Section 4.1. It was also noted that multiple craters formed on the cathode a few hours after the beginning of each test, and it will be shown in the next Section that this also provides evidence for the diffusion of gas from the liquid to the electrode surface.

(6) THEORY OF BREAKDOWN

The results obtained in this investigation suggest that the impulse breakdown of treated transformer oil is due to the emission of electrons from the cathode and their subsequent multiplication in the liquid. This general picture has been accepted for some time for pure liquid breakdown under both impulse and direct voltages, although for the latter it has been suggested that the breakdown strength is determined by whichever of the two processes is least probable. Thus, with uniform fields the strength is solely dependent on the process of electron multiplication in the liquid, and no cathode dependence is found.[14] Under impulse conditions, however, a dependence on the properties of both the liquid and the electrodes is to be expected, since breakdown is only a measure of the product of the probabilities of both processes occurring in a given time, although if the probability of one process occurring is considerably less than that of the other, the factors which affect this process will have the greatest influence on the breakdown.

Many of the details of the two basic processes involved in the breakdown mechanism are not understood, particularly in the more complex problem of breakdown in transformer oil. The results of the present investigation, however, have shown that the emission of electrons from the cathode is influenced by the presence of a gas layer on the surface of the electrode and it is intended to consider this aspect more fully, since it has not been discussed elsewhere in any detail.

At first sight it is difficult to see how any gas may remain on the surface of the electrodes when they are immersed in the oil. A chemisorbed layer will always be present, but this could not have been influenced by the vacuum techniques employed in the tests described and therefore could not have given the results reported in Section 4.6. On the other hand, layers of physically adsorbed gas would have been removed as soon as the electrodes came into contact with the oil. There will be absorbed gas in the bulk of the electrodes, but this could not influence the breakdown unless it migrated to the surface, which is unlikely. The only other possibility is for the gas to remain in equilibrium on the surface in the form of bubbles. A consideration of the relevant literature shows that in certain circumstances microscopic gas bubbles may be present, or may even grow on such a surface. The evidence is reviewed below.

A normal spherical bubble, stationary in a liquid and completely surrounded by it, will be slowly dissolved. The rate of solution may be calculated, and if account is taken of the surface tension at the gas/liquid interface it is found that the bubble will dissolve even in gas-saturated liquids, because the pressure inside the bubble is higher than that in the surrounding liquid.[15] However, if it were possible for the surface to be concave instead of convex, the pressure inside the bubble would be less than that in the liquid and the bubble might be stable in a partially degassed liquid. Such a situation is obviously impossible for a bubble completely surrounded by the liquid, but at an irregular liquid/solid interface a bubble could exist with a concave liquid/gas surface. It is suggested that such bubbles were present on the electrode surfaces at the beginning of each test unless the test cell was well evacuated before being filled with oil, and that in either case such bubbles were able to grow at the expense of the gas dissolved in the oil, although at a reduced rate when degassed oil was used.

The presence of such bubbles on liquid/solid interfaces has been verified by experiments with gas-saturated liquids. Since the solubility of a gas in a liquid is proportional to the pressure, reduction of the pressure above a gas-saturated liquid should result in gas coming out of solution. Normal bubbles, however, are unstable and dissolve even in gas-saturated liquids, as mentioned above, and therefore it would appear impossible for the gas to come out of solution. What actually happens in practice is that microscopic bubbles on the surface of the container or on solid particles suspended in the liquid become unstable and grow, thus releasing gas from the liquid. If these bubbles are removed by suitable treatment, very high degrees of supersaturation may be obtained without bubble formation.[16] Similarly, water may be heated to 200° C without boiling when microbubbles have been removed from the walls of the containing vessel.[17]

A further factor which may contribute to the equilibrium of gas bubbles on the electrode surface is the stabilizing effect of positive ions. In calculating the stability of small bubbles in a liquid, Martelli[18] has shown that the lifetime of such bubbles is very greatly extended by the presence of a single unneutralized positive ion inside the bubble. If this is applied to the problem of gas bubbles at a liquid/solid interface it is seen that the capture of a positive ion represents an additional stabilizing force.

It might be supposed that the main contribution to the gas on the surface of the electrodes was from the bubbles of gas which were sometimes formed by the decomposition of the oil by the discharge, and which appeared to touch the upper electrode as they rose to the top of the test cell. There is evidence to show, however, that these bubbles do not, in fact, touch the electrode. For example, in benzene a gas bubble of less than 1 mm radius released below a glass plate will not have risen to within 100 Å

of the plate after 5 min.[19] There could therefore be no direct transfer of gas to the surface in this way during the few seconds for which the bubble is near the electrodes.

Once it has been shown that microscopic gas bubbles may be present on the electrode surface, it is necessary to consider their effect on the breakdown. At first sight it might appear that the breakdown is initiated by the ionization of the bubbles, especially since the electric field in the bubble will be greater than the average field owing to difference in permittivities. It is more likely, however, that the main effect of the bubble is to cause enhanced emission of electrons from the underlying metal, owing to this local increase of the field, since the probability of an electron appearing from elsewhere in such a small volume is very small. That this is so—at least after conditioning—is borne out by the observed dependence of the electric strength on the metal of the electrodes. Even when an electron is emitted from the metal into a bubble, the chance of ionizing collisions occurring in the gas is small. The maximum possible value for the ionizing efficiency of electrons in nitrogen or oxygen is approximately 10 ion-pairs/cm/mm Hg, and for each ionizing collision the electrons would therefore have to travel at least 10 microns, which is of the order of the size of the bubbles.

The events during a series of breakdowns may now be considered more fully. Before the first breakdown a few microscopic bubbles will be present on the electrode surface unless special precautions have been taken. These bubbles assist the emission of electrons from the cathode and a low electric strength will result. If, on the other hand, the cell has been evacuated to 5 mm Hg or less before the oil is admitted, the bubbles will have been removed and a higher strength will be obtained which is independent of gas on the electrode surface. During the time taken by the subsequent breakdowns, the equilibrium of the gas on the electrodes is continually changing: some bubbles are removed by the discharges, some are dissolved, whilst others grow at suitable sites. As a result a conditioning effect is observed which leads to a steady breakdown strength which is independent of the initial conditions. This strength will depend on a number of factors, such as the testing rate, which determine the rate at which the bubbles are destroyed, and the gas content of the oil, which affects the rate of bubble growth. As the test proceeds a point will be reached when the strength again falls and the results become very erratic, owing to the accelerated growth of bubbles at certain favourable sites on the cathode—the sites where previous discharges have left pits. These will offer very irregular surfaces and free positive ions from previous discharges may be present, so that bubbles may grow very rapidly. As a result, after the lapse of a suitable interval for the bubble growth, breakdown may occur from these points again, thus causing the formation of multiple craters on the cathode. The local field intensification at these pits, arising from the irregular surface geometry, may also affect the electron emission, but this cannot be the only influence, since it cannot explain the fact that the steady strength after conditioning is higher than the strength of the subsequent erratic breakdowns.

The mechanism proposed here to explain the source of electrons for the initiation of the breakdown may well apply to the tests of previous workers. In particular, it could explain a pressure dependence of the breakdown strength without having to assume the process of electron multiplication in the bulk of the liquid to be dependent on the gas dissolved in the liquid. In this case, a time delay between the application of the pressure and the consequent variation in strength might be expected, and such a lag was reported by Hoover and Hixon.[20] It is not suggested that this mechanism does apply to tests of other workers, but rather that it is an explanation which has previously been neglected.

(7) CONCLUSIONS

It has been shown that the recorded impulse breakdown strength of filtered and partially degassed transformer oil is dependent upon the way in which it was tested. The recorded strength was dependent on both the manner in which the voltage was applied and the rate of testing. For a given test procedure the breakdown strength was dependent on the electrodes used, and, in particular, the effect of microscopic gas bubbles on the electrodes was studied. In general, the strengths obtained were higher and less erratic than those obtained by previous investigators with high voltages, and they were consistent with those obtained in recent years with small, carefully prepared test samples and lower voltages.

(8) FURTHER WORK

Since it has been found in these preliminary tests that consistent and reliable results may be obtained with the apparatus described, and that the effect of gas on the electrodes may be overcome for at least the first breakdown of each test, work is now being continued on other aspects of the electrode dependence of the impulse breakdown of transformer oil.[21]

(9) ACKNOWLEDGMENTS

The work was carried out in the High Voltage Laboratory of Queen Mary College, and one of the authors (R. H.) wishes to express his gratitude to the Department of Scientific and Industrial Research for a maintenance allowance which enabled the work to be undertaken.

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DISCUSSION BEFORE A JOINT MEETING OF THE MEASUREMENT AND CONTROL SECTION AND THE SUPPLY SECTION, 7TH JANUARY, AND BEFORE THE NORTH-WESTERN MEASUREMENT AND CONTROL GROUP AT MANCHESTER, 18TH FEBRUARY, 1958

Dr. J. B. Higham: The authors' work is particularly valuable because it is the first in which some of the refined techniques developed in recent years for use with small gaps and voltages up to about 50 kV have been adopted for use up to 500 kV.

Dr. Tropper and his colleagues at Queen Mary College agree on many things with my colleagues and myself, who until recently were in the Electrical Engineering Department of Birmingham University, but there are certain puzzling differences in the results which the two groups have obtained for apparently similar and crucial experiments. It is not surprising, therefore, that we arrive at different theories of breakdown. For example, the authors and ourselves have specially investigated the conditioning effect, believing that there are clues in it to the mechanism of breakdown.* Contrary to the authors' statement in Section 4.1, we found† that once conditioning had taken place the measured electric strength remained unchanged even during a pause in testing of two days. Furthermore, throughout our work on transformer oil we always found a conditioning effect, whether or not the oil was degassed. Near the end of Section 4, the authors refer to a reverse conditioning effect. Am I correct in assuming that this is a fall in electric strength as a function of the number of breakdowns? It is interesting if this has been found with transformer oil, because it would be for the first time, to my knowledge. On the other hand, we invariably obtained it with highly purified simple liquid dielectrics and very carefully prepared electrodes. Mr. Stark introduced it as a criterion of effective electrode preparation.

There is one further point about conflicting results; results not mentioned in this paper but which Dr. Tropper and his colleagues have obtained,‡§ and which are relevant to our theories of breakdown. They have found, under a certain few conditions, that the measured electric strength is independent of variations in the hydrostatic pressure applied to the liquid. On the other hand, we have always found an increase in strength with an increase in pressure; this applies for all conditions of liquid and electrodes and for voltages ranging from alternating and direct, right down to pulses lasting only 1 microsec.

* KAO, K. C., and HIGHAM, J. B.: 'Electric Breakdown of Dielectric Liquids', E.R.A. Report No. E/T67, 1956.

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‡ LEWIS, T. J.: Discussion at the Symposium of Papers on Insulating Materials, *ibid.*, p. 187.

§ MAKSIEJEWSKI, J. L., and TROPPER, H.: 'Some Factors affecting the Measurement of the Electric Strength of Organic Liquids', *ibid.*, Paper No. 1642 M, April, 1954 (101, Part II, p. 183).

Turning to the deductions from the experiments, we agree with the authors on three important points, among others. We agree that the breakdown process includes electron emission from the cathode and electron multiplication in the liquid and —at least in transformer oil—that bubbles play a part in the process. On the other hand, we disagree with the authors' theory that the principal role of the bubbles is to produce direct enhancement of the electron emission from the cathode and that bubbles are responsible for the conditioning effect. We believe that both these effects are caused by dielectric layers on the cathode. We also deduce that breakdown first occurs in a bubble even in pure liquids, and Dr. Hakim and I showed photographs in support of this at the Exhibition of the Third International High-Speed Photographic Congress.

The fact that some of the experiments by the two groups give conflicting results supports the point already stressed by the authors that measured electric strengths depend on the test procedure adopted. There must be reasons for the conflictions, inherent in the breakdown processes, and I feel sure that it would be very rewarding to make a deliberate search for them.

Mr. E. T. Norris: The liquid which the authors have been testing and have called transformer oil has little resemblance to that inside most transformers and circuit-breakers in service to-day. For chemically pure oil tested under laboratory conditions, strengths of 4 MV/cm have been measured; the authors have achieved about 900 kV/cm; oil passing the standard test in B.S.148 gives about 350 kV/cm, and the average oil in service gives something lower still. This is a very wide range of values, and I look upon the authors' work as helping to contribute to the narrowing of this compass. However, these variations in strength are due, not to the oil itself, but to the impurities in it, and so the authors' results (and those of other workers in this field) are really a measure of the impurities in the oil rather than of the intrinsic strength of the oil. In this light (and since impurities of a few parts in a million can have a large effect), it is not surprising that there are wide variations in the results obtained by different research workers and it probably explains some of the differences described by Dr. Higham. It is a curious reflection that such minute impurities will lower the electrical strength of insulation and raise the mechanical strength of metals by several orders of magnitude in each case.

Do the authors results follow a Gaussian or one of the extreme-value skew distributions? This would be of interest in

considering the effect of other electrode shapes and sizes on the mean values and standard deviations of breakdown.

There is an implication in Section 3.2 that a low standard deviation indicates an improvement in testing technique. This is not necessarily so. A needle gap will give good consistency and a low standard deviation, but owing to its insensitivity to impurities, an undesirable characteristic.

The authors state that conditioning occurred only with degassed oil. This is of some interest to engineers, because oil in transformers and circuit-breakers cannot be degassed since it is continuously exposed to air in service. It can be de-aerated, but that is a different matter.

Mr. C. G. Garton: I join Dr. Higham in welcoming the authors' extension of some very difficult techniques to longer gaps. It is not unusual, in this difficult work, for no two groups of experimenters to agree, and I feel that the case which the authors make in Section 6, for the importance of microscopic gas bubbles, is not quite convincing from a quantitative point of view. The authors avoid the difficulty that normal microscopic bubbles quickly dissolve through surface-tension forces by postulating bubbles in spots where the curvature of the surface is such as to reduce these forces. This, however, implies a bubble in a re-entrant angle, where the electric stress must also be reduced by the normal shielding effect of a re-entrant electrode. It is doubtful whether such bubbles should contribute to breakdown. A further quantitative difficulty is that the stress within a bubble is increased, at most, by a factor equal to the permittivity of the liquid—about 2 in this case. Larger increases than this should result from microscopic projections on the electrode surface, which, even if as blunt as a hemisphere, will multiply the stress by 3. Why does not the latter effect obscure the former?

The authors find that the effect of bubbles vanishes when the pressure is reduced from 20 to 5 mm Hg. It is difficult to see why this change to what is still a very poor 'vacuum' should be so effective. It may be significant that the range 20–5 mm Hg embraces the vapour pressure of water (17 mm Hg) at room temperature, and one may suspect the effect of an adsorbed water film on the electrodes, better removed at 5 than at 20 mm Hg. There is then an alternative mechanism which could enhance emission from the cathode. This is the presence of positive (H^+) ions, derived from the water and held by the field against an oxide layer on the metal. Until this possibility is excluded by experiment, I do not feel that the authors' explanation can be regarded as established.

Dr. L. L. Alston: When all the data given by the authors are considered, the mechanism they suggest appears to be the most probable. It is, however, not clear why one can conclude that conditioning is necessarily associated with the electrodes from the fact that oil was circulated in the cell between breakdowns (Section 5). If conditioning were associated with the bulk of the oil, circulating the oil in the test cell would increase the volume of oil to be conditioned, so that a conditioning process could still be present, although a larger number of flashovers would be required for a stable condition. It is stated in Section 4.6 that the flashover voltage decreased during conditioning if the electrodes were subjected to vacuum before the test. My first assumption was that this lowering of voltage was due to the generation of bubbles by the discharges, but from Section 6 it appears that bubbles generated in the oil cannot play an important part in the mechanism suggested by the authors. Again, bubbles generated at the electrodes (where discharges had rooted) could not have affected subsequent flashovers, for multiple roots were not observed during conditioning.

With reference to Section 4.5, would it be practicable to use plated electrodes if the surge diverter shown in Fig. 3 were included in the circuit?

The authors stress the dependence of electric strength on experimental technique, and some standardization of electrode configurations would facilitate comparison of results from different investigations. Stephenson profiles have much to commend them for uniform fields, and one or more sphere-plane gaps could be used as preferred configurations for non-uniform fields, in the same way as the 1/50 microsec wave is used for investigating impulse effects.

Mr. H. K. Beale: At first sight there would appear to be some discrepancy between Fig. 5 and Table 1. Since several breakdowns occur before a result is obtained with the first impulse technique, this must correspond to some point along the curve in Fig. 5, where the scatter of the results is increasing. Yet Table 1 shows a smaller coefficient of variation than with the normal technique.

Some work on organic liquids at the N.P.L. may help to explain this point. Fig. A shows the conditioning effect in a sample of di-methyl phthalate. The shape of the curve is in general agreement with Fig. 5 up to a point towards C. The scatter is almost constant from A to B, increases between B and C, and is small from C onwards. It may be that Fig. 5 would be similar, if continued, the first impulse technique yielding a value on the portion CD. Some information on the number of breakdowns occurring in this method would thus be of interest.

This conditioning effect has been observed at the N.P.L. only with untreated organic liquids. With purified liquids the effect is absent, as observed by other experimenters. It is interesting that this effect is apparently observed only in mineral oils and not in pure organic liquids. It is a point worthy of further investigation and one which should be accounted for in any theory of liquid breakdown.

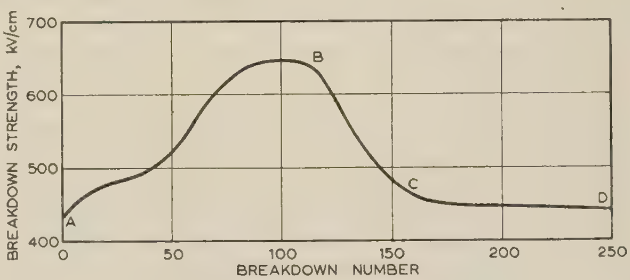


Fig. A.—Conditioning effect in di-methyl phthalate.

Mr. F. S. Edwards: The tank in Reference 1 was designed for high-voltage tests with alternating and direct voltages in addition to impulse voltages, and gaps of up to 24 in were employed. The authors' tank is stated to be suitable for gaps up to 6 in, but they did not use it at gaps above 6 mm. I think that these differences fully explain the large difference in the oil contents of the two tanks.

The authors give no information on the breakdown strength of their oil under alternating stresses; if they have any it would be useful if it could be published so that the impulse ratio of breakdown could be compared with that given in, say, Reference 2.

There are three references to time lag, but no numerical values are given (if we exclude the remark at the end of Section 4.3). Here again it would be useful for comparison with other published results if the authors could say more on this subject.

Mr. E. G. Wright: For the economic design of high-voltage

equipment it is important that the well-known variation in oil breakdown should be explained and, if possible, reduced. However, it hardly appears possible to control oil quality in commercial equipment to the extent necessary to get the results obtained by the authors, and other workers, under laboratory conditions. Even with such careful control, Fig. 5 shows the wide variation that still occurs due to conditioning effects. In this particular case the first breakdown is 30% lower than the conditioned breakdown. The authors mention in Section 5 that the oil strength for the first breakdown may be very important in practice, and indeed it is. For design purposes the breakdown strength for the first surge is required: equipment which breaks down on a surge is not being conditioned—it has failed.

The mean breakdown strength obtained varies between 500 and 900 kV/cm, and these values are similar to those obtained by other workers. For example, Endicott and Weber* using a rather different method obtained 720 kV/cm. As stated by the authors, even higher values have been reported, especially with smaller electrodes.

Unfortunately, the breakdown strength of commercial oil under normal test or service conditions is appreciably lower than these values. Under uniform field conditions a value of 400 kV/cm is typical for small gaps of about 0·1 in as used by the authors. For gaps between 1 and 2 in and a large electrode it is only about 200 kV/cm, as shown by the results presented at the discussion on Reference 12. The breakdown strength for sharp-edged electrodes can be less than half these values.

For design work a knowledge of the breakdown stress for practical gaps in commercial oil is required, for the stress can usually be estimated by mathematical or analogue methods. In my experience this stress is not just a function of electrode area, although the assumption by Wilson† that it depends only on the oil volume under stress may be too great a simplification. Further work on an extreme-value probability basis appears desirable, and I should like to know whether the authors expect to carry this out.

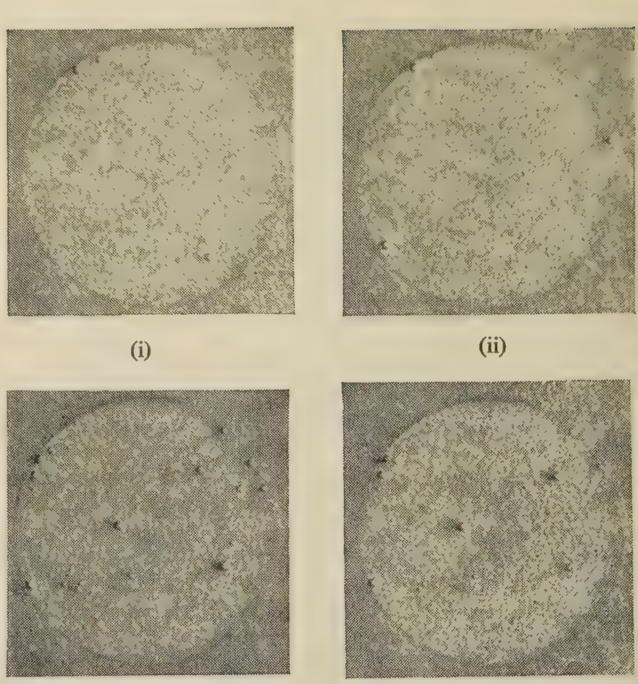
Mr. R. E. Wootton: The value of the information presented would be greatly enhanced if some indication were given of the number of tests upon which the mean values are based.

The breakdown strengths quoted in Section 4 are in decreasing order, given by the normal 2/60 and 2/600 microsec and the single-impulse techniques. This leads to the rather surprising conclusion that the conditioning effect is appreciably smaller with longer-tailed waves. It would therefore have been interesting had the authors quoted time-lags for the measurements with 2/600 microsec waves.

The authors conclude that conditioning is due to gas on the electrode surfaces, and an estimate of the bubble size is given. Was this estimate obtained by direct observation?

I have been concerned with the study of the effects of gas bubbles upon the impulse strength of oil-impregnated paper dielectric, and have constructed a small test cell which permits the direct observation of an oil-filled cavity in such a dielectric during impulse testing. This consists of three sheets of oil-impregnated paper (each 3 mils in thickness) with a ⅛ in diameter hole punched in the uppermost sheet to form the oil-filled cavity, adjacent to which is a thin transparent gold electrode.

When this arrangement is tested using the authors' 'normal' procedure, with a time interval of approximately 30 sec between successive impulses, no bubbles appear until an average stress of about 1 MV/cm is reached [Fig. B(*i*)]. After the following impulse (1·05 MV/cm), Fig. B(ii) shows that complete failure has not been initiated at the site of any of the original bubbles, as might have been expected, and that a spatial redistribution of these bubbles has occurred. With succeeding impulses these bubbles grow and fresh bubbles form. Fig. B(iii) shows the appearance of the cavity immediately after an impulse at a stress level of 1·3 MV/cm, and Fig. B(iv) shows that, after an interval of 6 min without further stress application, the smallest bubbles have dissolved while the remainder have diminished in size. Complete failure occurred at 1·5 MV/cm.



(i) (ii) (iii) (iv)

Fig. B.—Distribution of bubbles in cavity during impulse tests.

(i) 1 MV/cm.
(ii) 1·05 MV/cm.
(iii) 1·3 MV/cm.
(iv) After 6 min rest.

Mr. J. K. Webb: The authors have concentrated their efforts on developing a neat method for measuring the intrinsic strength of transformer oil. From the practical engineer's aspect, however, the real problem is to bridge the very large gap which he realizes exists between intrinsic and working strength. Unless the authors can suggest how this problem might be tackled, their paper would appear to have only academic interest.

Dr. K. C. Kao (*communicated*): I have found that the conditioning effect is also present for the oil containing dissolved gas. Moreover, it occurs even with rectangular pulse voltages of 10 microsec duration and highly polished stainless-steel electrodes. I have ascribed this effect to changes in a dielectric layer or layers on the cathode, which may be produced in the first place by the polishing operation, the cleaning process and by other treatments. This layer prevents positive ions from reaching the cathode and thus results in an intense local field at the cathode and causes high emission of electrons. It is the characteristics of the original and of the final dielectric layers on the cathode surface, together with the cathode micro-geometry, which appear to determine whether the measured electric strength rises or falls with an initial succession of breakdowns.

The authors suggest that the conditioning effect is due to the gradual removal of gas from the electrode surfaces by the breakdowns. It can be imagined that if the electrodes were

* Endicott, H. S., and Weber, K. H.: 'Electrode Area Effect for the Impulse Breakdown of Transformer Oil', *Transactions of the American I.E.E.*, 1957, 76, Part III, p. 393.

† Wilson, R. A.: 'Fundamental Factor Controlling the Unit Dielectric Strength of Oil', *ibid.*, 1953, 72, Part III, p. 68.

degassed by electric breakdowns, the electrodes would be gassed again when the liquid sample was replaced by a fresh one after the conditioning had been completed, so that conditioning should commence again. But I found that this was not so: once the conditioning was completed, it would not resume even if the liquid sample was replaced with a fresh one.

I realize that the presence of gas bubbles on liquid-solid interface is possible, but it seems unlikely that this is the main cause of conditioning effect, since it cannot explain the facts mentioned above. It is known that the experimental conditions may greatly influence the experimental results; have the authors noticed whether the conditioning will resume if the electrodes remain unchanged after it has been completed and the test cell is not evacuated before being filled with a new oil sample, so that tiny bubbles would exist again on the electrode surfaces?

Mr. W. P. Baker (*at Manchester*): The invocation of field emission as a preliminary to breakdown appears to be beoming increasingly popular, and it would seem to be worth while considering the authors' suggestion in more detail.

It is claimed that the stress at the surface of the electrode is enhanced by a bubble attached to the surface. For stability the centre of curvature of the gas-liquid interface must be on the liquid side of the interface; such a condition can exist in a crevice in the electrode surface. It is reasonable to assume that such a crevice would have a roughly triangular cross-section, and the enhancement of stress due to change in permittivity from oil to gas would, in fact, be roughly cancelled by the diminution of stress due to the indentation.

It is very difficult to discuss field emission in quantitative terms, because the thermionic work-function of an alloy or an oxide is not known accurately, nor is the modification of the work function by the presence of the liquid. A partial solution to this problem might result from the use of electrodes which neither oxidize nor are alloys, namely electrodes of platinum or gold. Solid electrodes of noble metal are, of course, expensive, but gold plating is not prohibitively so.

For this reason, it is saddening to note that the authors did not consider the use of the surge diverter worth while, when the amount of damage to the electrodes could be reduced by its use to a level that would enable plated electrodes to be used.

Dr. R. Hancox and **Dr. H. Tropper** (*in reply*): We agree with Dr. Higham that there are differences between the results of our two groups, but it seems that there are also inconsistencies in their results. For example, to illustrate that conditioning once completed is permanent (a point also made by Dr. Kao), Dr. Higham states that the measured electric strength remained unchanged even during a pause in testing of two days. However, he has also reported a test* where a strength of 1 100 kV/cm was obtained after conditioning, but fell to 700 kV/cm after the oil sample was placed in contact with dry filtered air for 24 hours.

Unlike Dr. Higham and his colleagues, we believe that the two processes involved in the electrical breakdown of treated oil are cathode emission, which supplies the liquid with an adequate number of electrons, and an electron multiplication process in the bulk of the liquid. This general physical picture for the breakdown was suggested some time ago† and is in good qualitative agreement with many of the experimental results obtained in this laboratory and elsewhere. Accordingly, as stated in the paper, the breakdown strength will be determined by whichever of the two processes is least probable. If, for example, the cathode emission is less likely, a higher electric field will be required for this process than for the electron multiplication in the liquid, and the breakdown voltage will depend on the conditions of the cathode, and on the liquid only in so far as it affects this electrode. A breakdown of this type will always occur whenever a conditioning effect is present, and since the breakdown strength depends on hydrostatic pressure, the cathode-emission process must be pressure dependent. The presence of microscopic bubbles can explain such a pressure dependence. We cannot agree with Dr. Higham and Dr. Kao, who ascribe conditioning and electron emission to dielectric layers on the cathode, for it is difficult to see how these layers can account for the observed pressure dependence.

There is definite experimental evidence for the presence of air on the electrodes. Under suitable reduced pressure the evolution of air bubbles from the cathode can be observed for uniform field configurations and for an applied direct voltage which is well below the breakdown voltage. Also, it has been found* that conditioning is accompanied by a gradual degassing of the electrodes. Furthermore, recent tests on carefully prepared oil samples of varying air content have shown that conditioning depends on the air content of the oil. For an air content corresponding to a given equilibrium pressure, conditioning was found to be more pronounced when carried out at a high pressure, when the oil was under-saturated, than at a pressure near the equilibrium pressure, when the oil approached saturation. This provides independent confirmation of the result mentioned in the paper that conditioning was absent for gas-saturated oil, i.e. after prolonged bubbling of nitrogen through the oil sample.

We agree with both Mr. Garton and Mr. Baker, who doubt that the electric field inside a bubble will be much greater than the average field, owing to the difference in permittivities, and admit that our explanation is an over-simplification. Recent experiments have shown that the classical picture of bubble formation and stability which was invoked must be modified in the presence of an electric field and charges which have a marked effect on the surface tension and the wetting properties of the oil. Taking these effects into account, it appears more probable that the sites of the microscopic bubbles act as centres of regions where the oil has become detached from the electrode. For these enlarged regions the field intensification due to difference of permittivities would apply as suggested, and there may be further emission from microscopic projections on the electrode in these regions, as mentioned by Mr. Garton. The presence of positive (H^+) ions on the oxide layer of the metal, which are derived from moisture as suggested by Mr. Garton, cannot be ruled out by experiment.

We do not think that the use of electrodes which neither oxidize nor are alloys, as advocated by Mr. Baker, is justified in oil tests. The work function is extremely sensitive to surface layers, so that the effective value would bear little relation to the vacuum value.

The use of a surge diverter, referred to by Dr. Alston and Mr. Baker is not as effective for impulses as for long-duration voltages, since the resistance in series with the test cell must be small for short wavefronts. Even in small-scale tests (small electrode capacitance) with operation times of the surge diverters of less than 1 microsec, the pitting is appreciable, and precludes the use of plated electrodes, although oil decomposition is reduced, as was also found in the present tests.

We agree with Mr. Norris that, unlike the needle gap, oil breakdown is extremely sensitive to minute traces of impurities, and therefore fail to understand why he should query a low standard deviation as a criterion of the quality of the testing technique. The tests were lengthy and hence not enough measurements are available to establish the nature of the distribution.

There is no discrepancy between Fig. 5 and Table 1, as sug-

* Watson, P. K., and Higham, J. B.: Discussion at the Symposium of Papers on Insulating Materials, *Proceedings I.E.E.*, 1953, **100**, Part IIA, p. 188.

† Zein El-Dine, M. E., and Tropper, H.: 'The Electric Strength of Transformer Oil', *Proceedings I.E.E.*, Monograph No. 135 S, June, 1955 (**103 C**, p. 35).

* Maksiejewski, J. L., and Tropper, H.: 'Some Factors affecting the Measurement of the Electric Strength of Organic Liquids', *Proceedings I.E.E.*, Paper No. 1642, April, 1952 (**101**, Part II, p. 183).

gested by Mr. Beale. It is clearly stated that the measurements were made at 30 min intervals in one case and at 5 min intervals in the other, and the effect of this is explained. The required information about the number of breakdowns is also given in the paper.

In reply to Mr. Edwards, the longest gap tested was 2·75 cm, and the spark lags measured during tests were of the order of 7 microsec. No tests were made with alternating voltages, but in a parallel investigation* using a large-scale technique and direct voltages, an electric strength of 400 kV/cm was obtained for the same electrodes and gap setting.

* JAYASEKARA, W. P.: 'The Breakdown of Treated Transformer Oil when subjected to Direct Voltages up to 200 kV', Ph.D. Thesis, University of London, July, 1957.

The pictures of bubble formation shown by Mr. Wootton are very interesting but have little bearing on the paper, since conditions are different. We did not expect to see bubbles and in any case lacked the courage to look for them. As to the number of tests, every result is based on at least four series of tests, each providing at least five results, so that never fewer than 20 measurements were considered.

The brief answer to Mr. Webb is that the paper is intended as an initial attempt to bridge the gap to which he refers.

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RECENT DEVELOPMENTS IN MEDIUM-VOLTAGE H.B.C. FUSE LINKS

By R. H. DEAN, B.Sc.Tech., Member.

(*The paper was first received* 25*th April, and in revised form* 12*th June*, 1957. *It was published in October*, 1957, *and was read before the* SOUTH-EAST SCOTLAND SUB-CENTRE 5*th November, the* SOUTH-WEST SCOTLAND SUB-CENTRE 6*th November*, 1957, *the* NORTH-WESTERN UTILIZATION GROUP 14*th January, the* UTILIZATION SECTION 16*th January, the* NORTH MIDLAND UTILIZATION GROUP 21*st January, and the* NORTH-EASTERN CENTRE 27*th January*, 1958.)

SUMMARY

The paper reports development work carried out in the investigation of fuse links of ratings up to 600 volts and 600 amp in respect of basic problems of design when compliance with North American specifications and general circuit requirements is sought. Although this work was of a fundamental nature for universal application, it was stimulated by the publication of the Canadian Standards Association Code 106. Comparisons are made between this Canadian Code, the 'Standard for Fuses' used in the United States, and current practice in Britain, which centres on B.S. 88 : 1952. Fundamental difficulties in fuse-link design are set out, leading to the choice of a design to satisfy general circuit requirements and the C.S.A. Code No. 106 in particular. Details of performance are given of the resulting range of fuse links. The results of the high-breaking-capacity tests carried out by the Canadian Standards Association are compared with the calculated figures.

Consideration is given to consistency of performance, and tests to demonstrate non-deterioration under service conditions are described.

A novel method is given for the rapid estimation of the effect of the fuse link in limiting fault power and energy admitted on severe fault conditions.

SYMBOLS AND EXPRESSIONS

t_0 = Pre-arcing time.
t_v = Virtual pre-arcing time.
I_0 = R.M.S. prospective symmetrical fault current.
K_p = Pre-arcing constant.
J = Current density.
i_p = Peak value admitted or 'let-through' current (see Fig. 1).
Fusing factor (of a fuse) = The ratio, greater than unity, between the minimum fusing current and the current rating.

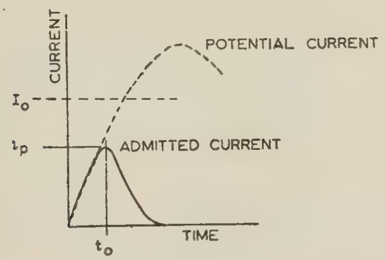

Fig. 1

Comparison of Terminology

British	*North American*
High breaking capacity (h.b.c.).	High rupturing capacity (h.r.c.).
Fuse link.	Fuse or cartridge fuse.
Motor starting current.	Inrush current.
Discrimination.	Co-ordination.
Admitted' or 'cut off' current.	'Let-through' current.
Non-rewireable fuse link.	One-time fuse.

Mr. Dean is with Parmiter, Hope and Sugden, Ltd.

(1) COMPARISON OF NORTH AMERICAN AND BRITISH REQUIREMENTS

In Great Britain the requirements of cartridge fuse links for use in industry and by supply undertakings are specified in B.S. 88: 1952, and are usually to Class P, having a fusing factor not exceeding 1·25, or to Class Q, having a fusing factor not exceeding 1·75.

In Canada they are covered by the C.S.A. Codes 59 and 106: the former was written in the days of small generating and transforming stations, and the only rupturing capacity specified is 10 kA at 600 volts d.c.; the growth of potential short-circuit currents resulted in the issue, in 1954, of Code 106, based largely—so far as the rupturing-capacity tests of 80 kA at 600 volts were concerned—on British techniques. The different minimum-fusing-current and time-lag customs of Britain and Canada were covered by specifying two classes of fuse link: Form I is similar to Code 59, but with the required rupturing capacity generally similar to Class P of B.S. 88, and Form II similar to Class Q of B.S. 88. Form I is permitted for general use in any situation, but Form II is for breaking capacity only, chiefly for back-up protection. The enforcement of the regulation is carried out by the inspection staff of the supply undertakings, and apparatus for connection to the supply has to bear the approval mark of the C.S.A.

In the United States the Underwriters Laboratory Incorporated issue a 'Standard for Fuses' which is nearly identical with the C.S.A. Code 59, but they have no standard for high-breaking-capacity fuses. These regulations are virtually enforced by the fire underwriters and insurance companies, who will not generally insure an installation unless such items as fuses bear the mark of approval.

The difference in the specialized requirements for the United States and Canada compared with those for Great Britain are both physical and electrical. To comply with the North American physical dimensions is not difficult, since they are comparatively large. The electrical differences are, however, of a more complex nature, and may be grouped under two headings.

(1.1) Overload Protection

The first heading covers the capacity of the fuse to operate on small continuous overloads after a time lag. North American regulations insist that a fuse should protect the cable and apparatus it serves on all overloads which could cause damage; they insist in particular that the fuse should rupture on 135% full-load current, and specify the time within which rupture must occur. To ensure a suitable time lag, the time within which each rating of fuse must rupture on 200% full-load current is also given, and these two requirements effectively govern, in practice, the time/current characteristics of the fuse, so that adequate time lags are provided.

B.S. 88: 1952 also envisages two classifications of fuse: Class P, with a fusing factor not exceeding 1·25, and Class Q, having a fusing factor not exceeding 1·75. Semi-enclosed fuses are assigned a fusing factor of 2. No time lags for the passing of safe overloads are specified. The duration of test quoted in

Table 3 of B.S. 88: 1952, giving the time within which the fuse link must operate, demonstrates minimum fusing current, but gives no indication of the capacity of the fuse link to sustain safe overloads, first because only the maximum time is specified and secondly because the time/current curve is practically asymptotic in this region.

The Institution's Wiring Regulations give no instructions as to the classification of fuse to be used, and it is possible that some of the fires reported by Gosland[1] would have been avoided if a minimum operating overload of 1·35 times the rating of the fuse link had been insisted on, as in North America.

(1.2) Breaking Capacity

The second point of difference is in the requirements for breaking capacity. The maximum specified in B.S. 88 is Category AC5, consisting of a prospective current of 46 kA symmetrical on 440 volts, while Code 106 specifies 80 kA symmetrical or 100 kA asymmetrical at 600 volts. Customary procedure in Canada and the United States is to refer to the asymmetrical wave; this includes the d.c. component, and when the degree of asymmetry is not specified, the ratio between the symmetrical and the asymmetrical currents is fixed arbitrarily. The Canadian specification gives the ratio as 1 : 1·25 (United States sources frequently assess this as 1 : 1·4). Both the Canadian and British specifications demand similar tests on a prospective current reduced to the value at which current limitation by the fuse link commences, since it is estimated that there is a maximum release of energy in the fuse link at this point. It is worth noting that there is a probability that, in fuses of modern design, maximum-breaking-capacity conditions are no longer the most onerous, and that, after the point at which current limiting takes place, the forces released in the fuse link decrease as the potential current available increases. Minor points of difference in conditions of test are as much a problem for the testing authority as the designer.

Code 59 and the 'Standard for Fuses' specify only 10 kA at 600 volts d.c.

(1.3) Other Characteristics

Not directly specified are three additional desirable characteristics, namely

(*a*) The fusing time/current characteristics of the fuse link should bear some relation to the operating curve of the apparatus it is likely to protect. The 600-volt range of fuse links will be used almost invariably for motor services, which generally have thermal-overload protection. The fuse link must discriminate with such devices, and the characteristics are arranged so that the fuse link will take precedence on currents which are greater than 6–10 times full-load current, while the relay will operate before the fuse on lower values covering all overloads up to the locked rotor current. Time lag is therefore required up to about 10 times the rating of the fuse link. Any current in excess of this must be a fault current, and the fuse link, in order to limit the energy allowed to flow in the circuit, should operate as rapidly as possible. Fig. 2 illustrates this point. The operating time/current characteristics of different makes of overload relay vary, and individual relays are likely to deviate appreciably from published average statistics. Allowance must be made for this.

(*b*) The heat loss by the fuse link on normal currents must be kept as low as possible. In order to make the fuse link rupture on 135% of its rating, many North American manufacturers using a single-metal-element construction must allow for temperature rises up to the maximum permitted. In view of the large dimension of the fuses, the energy released as heat is considerable, and because of this the C.S.A. Specification No. C.22.2, No. 4, which covers fuse switches, specifies that all tests to determine compliance with temperature rise shall be made with the fuse links replaced by dummies (Clause 118). To make allowance for the heat released by the fuses, further requirements insist that, using single-metal-element fuses to Code 59, the fuse switch shall be used on currents of less than one-half to one-third the rated capacity on the label. For example, a 30 amp fuse switch with 30 amp fuses is permitted for

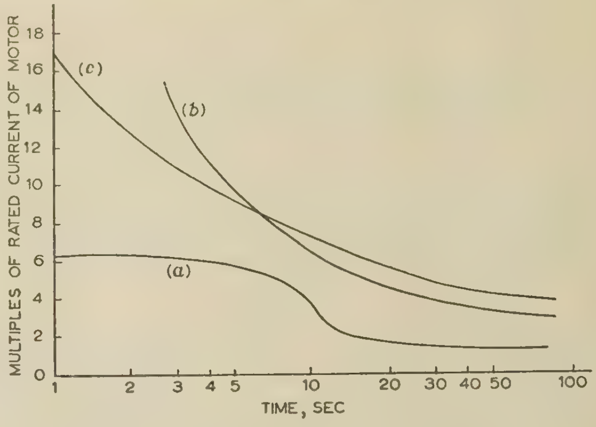

Fig. 2.—Typical time/current characteristics.

(*a*) Motor starting current.
(*b*) Thermal-overload relay.
(*c*) Discriminating fuse-link.

use on motors not exceeding 7½ amp rating. Dual-element fuses, which create less heat, permit the fuse switch to be used up to two-thirds of the label rating.

(*c*) On severe short-circuits the time taken from the commencement of the fault to the opening of the circuit shall be as rapid as possible, consistent with the consequent voltage surge, or arc voltage, being safely within the 2 kV permitted by C.S.A. Code 106. This limits the current, and consequently the energy permitted to flow in the circuit by the fault, to a fraction of that which would flow were the protection device of the non-limiting type, such as a circuit-breaker. It should be noted that the effective limitation of energy varies as the product of the square of the current limitation and the relative time of operation. This is amplified in Section 6.

(2) BASIC DESIGN CONSIDERATIONS

From this survey of the more important differences between North American and British practice, the work carried out to design fuse links to suit North American conditions will now be considered. So far as Canada is concerned, this meant satisfying the Form I class of the C.S.A. Code 106, together with the additional features mentioned in Section 1.3. It involved a study of the time of operation from the long-time fusing ratio specified (1·35) to a breaking capacity of 80 kA symmetrical at 600 volts, and in the event, produced a fuse link which, having a fusing factor of 1·25, also complies with Class P of B.S. 88. Table 1 compares the two requirements for a number of ratings.

Two ranges of physical dimensions are provided, the first to replace immediately any existing type of Code 59 fuse and the

Table 1

Similarity between B.S. 88 Class P Minimum Fusing Current and C.S.A. Code 106 Form I Minimum Fusing Ratio

Fuse links to B.S. 88 Class P					Fuse links to C.S.A. Code 106 Form I	
Size	Fusing factor of 1·25	Current at which fuse link will not blow	Current at which fuse link will blow	Maximum time to blow	Fusing ratio of 1·35	Maximum time to blow
amp	amp	amp	amp	min	amp	min
30	37·5	35·5	39·4	150	40·5	60
60	75	71	79	180	81	60
100	125	118·3	131·5	240	135	120
200	250	236·6	263	300	270	120
400	500	474	526	360	540	120

second to take advantage of modern techniques and reduce the size of fuse link. This permits a reduction in size of enclosures and mountings.

At the outset the fuse designer is confronted with a problem of producing a fuse element which will cater for two sets of contradictory requirements. High breaking capacity is obtained by limiting the energy released in a fuse, by rapid operation in a fraction of a cycle and the consequent limitation of current. Time lag is obtained by providing a heat reservoir to delay the operation of the fuse until the desired overload energy has been passed.

The speed of operation when breaking capacity is considered involves the basic problem of minimizing the mass of metal to be volatilized before arcing commences. Only metals of the highest conductivity can therefore be considered, which, in effect, limits the choice to silver and copper. The difficulty with these metals is that their melting points are 960 and 1 083° C, respectively, and the temperature rise of a fuse having a simple element of either of these metals would, if in any degree close rated, be too great for normal enclosures and would certainly not fulfill the temperature-rise requirements. Furthermore, the time lag to suit such conditions as motor-starting overloads would be inadequate. A similar problem is experienced when copper fuse wire is used in semi-enclosed fuses. Any attempt in practice to reduce the minimum current at which such a fuse will rupture below twice the rating of the fuse causes the wire to operate at such a temperature that oxidation takes place; the wire thus deteriorates, and ageing occurs.

Time lag is obtained by the use of a metal having a relatively high resistance, so that the mass of metal for a given rating is correspondingly greater. Zinc, aluminium, lead and tin–lead alloys, in simple designs, have been used for this purpose. Advantage can be taken of the lower melting point of these metals, so that a fuse complying with the North American minimum fusing ratio of 1·35 can also comply with their temperature-rise requirements. The amount of metal involved under short-circuit conditions is excessive, however, and the fuse has insufficient breaking capacity.

Two practical solutions of reducing the temperature rise under overload conditions in a high-breaking-capacity fuse have been found, namely

(*a*) The dual element fuse link (Fig. 3). This consists of two portions in series, one to provide high breaking capacity and the second to operate at a low temperature on long overloads and provide close rating. In practice, there are usually three zones, comprising a time-lag element mounted between two high-breaking-capacity elements, all mounted in series. This design can be made to operate, if required, on currents as low as 110% of the rated current.

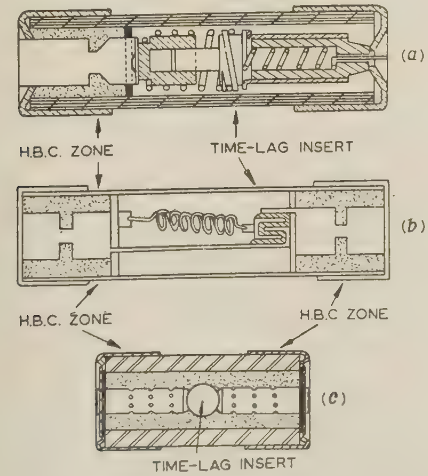

Fig. 3.—Dual-element fuses.
(*a*) Small-capacity North American.
(*b*) Large-capacity North American.
(*c*) British.

(*b*) An alternative system uses a natural phenomenon which has become known as the M-effect (Fig. 4), in which a plug of low-

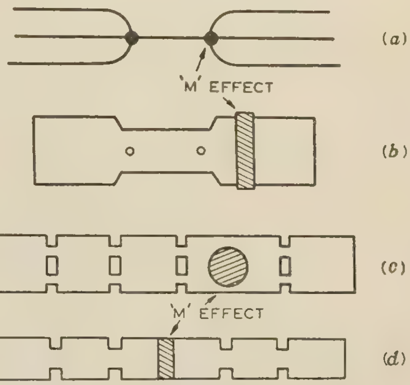

Fig. 4.—Elements using the M-effect.
(*a*) Silver-wire element.
(*b*)–(*d*) Silver-strip elements.

melting-point alloys with a wire or strip fuse element of silver when its melting point is reached. This silver alloy has a lower conductivity than the original silver, and the increased heat generated coupled with the reduced melting point forms a zone of operation at which the fuse ruptures. By this means, fuse links having an element of silver melting at 960° C can be operated under overload conditions without the temperature of the silver having been raised above the melting point of the alloy plug, which is probably about 200° C. This device gives a degree of time lag and close rating. Almost all modern designs of simple high-breaking-capacity fuse links use this principle in one form or another. With this device a fuse link is, in general, made to operate on 160–180% of the rating, but the time lag provided is thought to be inadequate. Some doubt is also expressed concerning the satisfactory operation of the fuse on currents between the full-load current and the minimum rupturing current, and on such overloads as motor starting. This latter type is, in general, the basic design of Form II fuses to C.S.A. Code 106, and of Class Q of B.S. 88.

The first reference to the M-effect seems to have been in 1939, by Metcalf,[2] from whose initial the name has apparently been taken. Very little quantitative information has been published.

Fig. 5 shows time-lag effects for operating times above 1 sec obtained with different types of fuse link. For simplicity, the comparison is made between links of 60 amp rating.

(3) THE DUAL-ELEMENT SOLUTION

The developments with which the author has been associated have been on the dual-element type of fuse link, which satisfies all the required conditions, especially Form I of C.S.A. Code 106. Furthermore, very considerable experience has been gathered over many years in the testing and manufacture of fuse links to Class P of B.S. 88, for which the conditions are very similar.

In the design of a fuse link for such conditions, two items—the element and the container tube—differ substantially from current practice; these will be described in detail.

(3.1) The Element

Fig. 6 shows the element used, and it will be seen to consist essentially of two parts, the high-breaking-capacity (h.b.c.) zones and the low-temperature time-lag zones.

The former is made from copper strip, tinned and perforated to form a constriction for rupture on a severe fault. The use of such a perforation is original, and a factor contributing to its

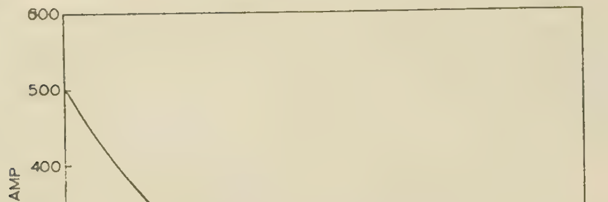

Fig. 5.—Time/current characteristics for various types of 60 amp fuses.

——— Dual-element.
– – – No. 17 s.w.g. copper wire.
— · — M-effect.
—··— Zinc rewireable.

Fig. 6.—Dual-element strip.

use is the knowledge, developed by testing various elements over many years, of the minimum length of 'neck' or gap such that an arc created across it will be extinguished with the greatest rapidity consistent with desirable limitation of arc voltage. With two zones in series there are, on operation, two parallel circuits, each having four arcs in series. The diameter of an individual perforation is about 0·06 in and the width of the neck is 0·015 in. Such a system of perforations in a flat strip was chosen because the amount of copper involved in operation is less than in other designs, since the cooling effect by axial conduction of the strip immediately adjacent to the holes is greater. Of lesser importance is the fact that a greater area of this part of the element is in intimate contact with the powder, which accepts heat by conduction. The net result, on conditions of continuous loading, is that, for a given temperature rise in the necks, the current density can be increased and the amount of metal reduced. The pre-arcing time on operation varies directly with the mass of metal to be volatilized. The quicker the operation, the greater the limiting of the admitted fault current. For these reasons, a flat strip with perforations produces the greatest current-limiting effect.

This limiting effect is described in more detail in Section 6, but the following figures illustrate its magnitude. On a prospective current of 100 kA asymmetrical, a 30 amp fuse link operates in 0·7 millisec with an admitted current of 6·0 kA (peak), while the 600 amp link operates in 5 millisec with a 47 kA peak admitted current.

In the design of the centre insert, i.e. the device to operate on overload conditions and at low temperatures, a system has been developed which does not use springs—a common practice in United States designs. Springs and moving parts are better avoided in a device such as a fuse link, which must remain in service for years without testing or inspection. Essentially this system consists of a plug of low-melting-point alloy, and it has been used in one form and another for many years. The plug is a tin-lead eutectic alloy melting at 180° C; it is cast in the solid in the larger sizes, and is formed from tubing in the smaller, and it is joined to the h.b.c. zones of the element by soldering. The heat for its operation comes mainly from the heat generated in the h.b.c. zones, and only partly from its own resistivity. The design of this centre insert and the method of mounting it between the copper strips must be such that the heat from the h.b.c. zones enters the plug of metal at an even temperature. A wide area of soldered joint along its periphery further provides even acceptance of the heat. The use of perforations—a series of small points at which heat is generated—to form the h.b.c. zones ensures that, so far as possible, there is a uniform temperature across the width of the element. This design of element is thus superior to one in which the h.b.c. zones are formed by single narrowings or 'necks'. The centre insert consists of a cylinder of tin-lead alloy sealed in a casing of a fluxing agent, boric acid. Before the melting temperature of the alloy is reached the adjacent boric acid melts and forms a liquid contained by the unmelted periphery. When the alloy melts, gravity and electrical forces cause it to scatter as globules which become embedded in the liquid flux. The current is then broken in two ways: by physical separation of the current-carrying alloy into globules, and, since the boric acid in which they are embedded is an insulator, by electrical isolation.

It has been noted that the heat energy created by the fuse on load must be kept low. Modern techniques in Great Britain now permit an element of shorter length and lower resistance than those in common use in North America. This has been achieved by the use of copper, having a relatively high conductivity, instead of zinc-based elements, incorporated in a dual element whose centre has low current density. Furthermore, a feature in the design of the element is the reduction of the distance between the h.b.c. zones and the centre insert. This reduces the loss of heat during transmission from its source at the h.b.c. perforations to the point of application in the centre insert. With this more efficient design, less heat is needed for operation. In the design considered the element is one-third to one-half the standard length, and its resistance on full load is about one-half that of the common single-metal North American design.

This reduction in size enables two ranges of fuse to be provided—one that is interchangeable with North American fuse links of similar rating, and one having shorter barrels but standard base contacts at reduced centres.

(3.2) The Container

The containing tubing or barrel must be inert and able to withstand both the high bursting pressure and the intense heat from the operation of the fuse generated by short-circuit conditions without fracture by unequal expansion. The end of the tube must be ground to fine limits, so that the end cap can be expanded on to it. Tubing to meet all these conditions is readily available in a ceramic material. Bonded glass fibre or Melamine tubing was not used, chiefly because of expense and supply difficulties, and also because the bonding material tends to change state on severe heating.

(4) CHARACTERISTICS AND TESTS

The detailed working out of dimensions for each rating involved highly intricate experimental work. Many data had to be obtained to determine the effect of each of the many factors involved, and this was complicated by the fact that each separate factor was to a greater or lesser extent interdependent on the others. A complete analysis was impossible, owing to

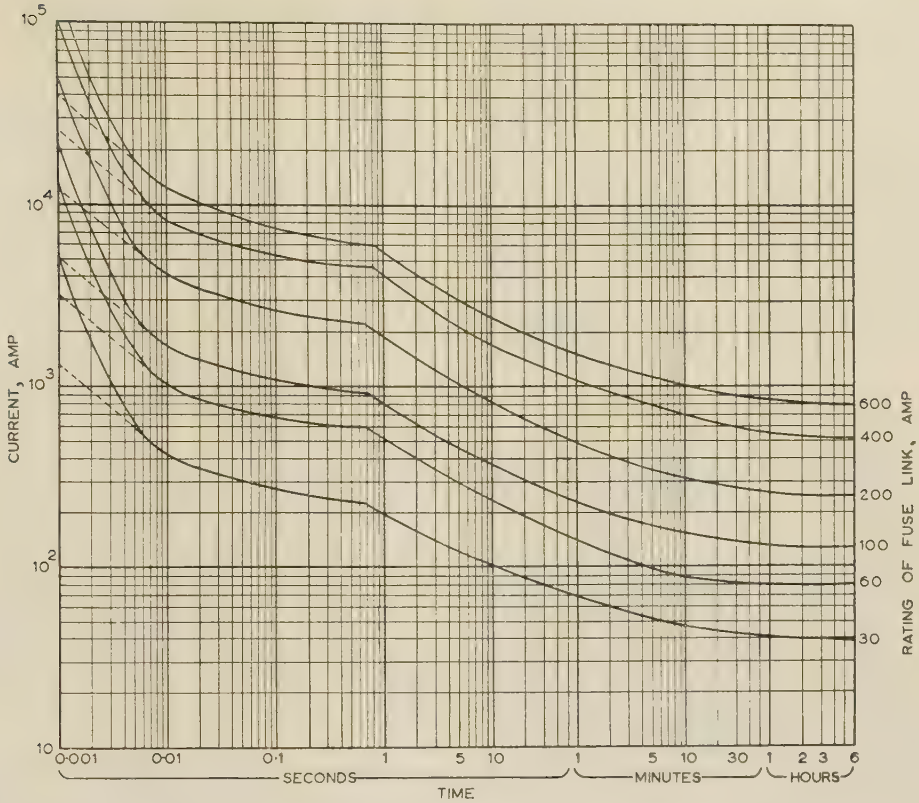

Fig. 7.—Time/current characteristics of dual-element fuse links.

——— Pre-arcing time on symmetrical a.c. fault.
– – – – Virtual pre-arcing time.

ıme and expense, and its place was therefore taken by shrewd ɛduction from many years of experience and testing. For this ɛason little more can be reported beyond the stating of the basic rinciples governing design. The range of fuse links produced ɛceived approval to C.S.A. Code 106 Form I.

The fusing characteristics of the range are shown in Fig. 7, ne discontinuity in each curve occurring at the point of change-ver from operation in the centre insert to operation in the .b.c. zones. This is arranged to occur at approximately ten imes the rating of the fuse, up to which point time lag is provided o that a motor protection device, such as thermal overloads, an operate in precedence. At currents above this the slope of ne curve decreases immediately, so that further increases in ault current produce disproportionately large decreases in perating times.

The performance curve for heavy currents (Fig. 8) shows the elation between the prospective fault current and the peak dmitted current, and is of value in ascertaining the magnetic tresses created by fault conditions. Further consideration is iven to this curve in Section 6. It was prepared from theoretical onsideration and confirmed by test.

The performance of a fuse for these heavy prospective currents calculated from a knowledge of the known physical constants f the element involved. It has been found in practice that, w ng to the short time taken for the element to fuse, i.e. the re arcing time, the loss of heat by conduction or radiation can e neglected, or a small factor allowed. It can therefore be ss med that all the energy transformed into heat by the resis-n e of the element is used to raise its temperature until s on occurs. The physical constants of a particular ele-t being fixed, the amount of energy required to raise the element to fusion is expressed in the form $\int_0^{t_0} I^2 dt$. This function is called the 'pre-arcing constant', K_p, and from it the pre-arcing times and peak let-through currents for different prospective currents are calculated. It can be taken as constant for any particular design of fuse.

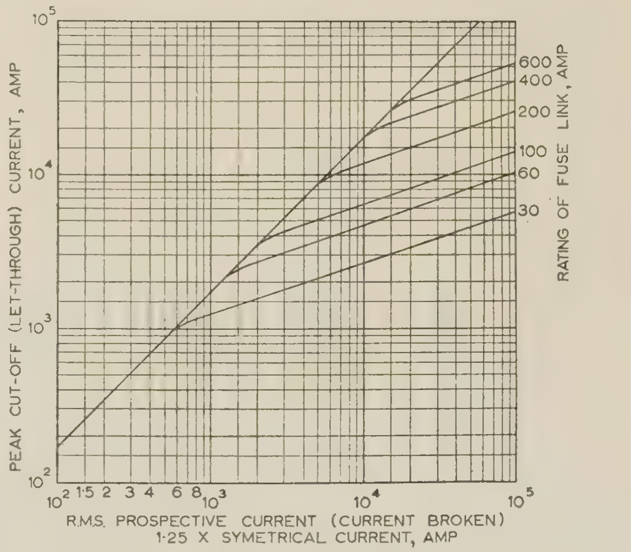

Fig. 8.—Relation between peak cut-off current and r.m.s. prospective current.

Its calculation has been shown by Gibson.[3] Briefly, $\int J^2 dt$

(where J is the current density for the element metal required to produce fusion, assuming no loss of heat) is computed by equating the heat used in raising the metal to fusion to the heat generated by its own resistivity. This gives a differential equation, from which $\int J^2 dt$ is $3{\cdot}3 \times 10^{10}$ (amp/in^2)^2-sec for silver and $4{\cdot}35 \times 10^{10}$ (amp/in^2)^2-sec for copper. The most recent figures published by the E.R.A. for specific resistance, specific heat, density, etc., were used.[4] This figure, multiplied by the square of the cross-sectional area of the narrowest part of the fuse element, gives K_p, convenient units being amperes squared-seconds.

The pre-arcing times for a given prospective current are obtained from the solution of

$$K_p = \int_0^{t_0} 2I_0^2 \sin^2 \omega t dt$$

The current wave admitted under these severe conditions is shown in Fig. 1. The pre-arcing trace approximates to a sine wave and so the peak value of the admitted or let-through current is $i_p = \sqrt{2}I_0 \sin \omega t_0$.

It is interesting to note that asymmetrical conditions, which would alter the shape of the current tracing, do not, in fact, occur. To comply with the C.S.A. Code 106, the test circuit must be closed at such a time that arcing commences at between 60 and 90 electrical degrees in the voltage wave. With heavy prospective currents the pre-arcing time (for the design being considered) corresponds to a few electrical degrees, and the angle between voltage zero and the point at which the circuit is completed—the closing angle—is such that the voltage/current trace associated with a power factor specified not to exceed 0·2 will be substantially symmetrical.

The results of the tests carried out by the C.S.A. are set out in Table 2, together with the equivalent figures arrived at by calculation using the method just described. The comparison affords a high degree of confirmation of the calculation and hence the predictability of performance. It also demonstrates the accuracy of manufacture, since the test fuse links must be picked at random from a batch.

The broken extensions of the curves in Fig. 7 are described as showing 'virtual pre-arcing time'; this is a theoretical quantity useful in confirming the provision of adequate heat capacity in cables and busbars, and also for comparing fuse links for discrimination. It is defined in B.S. 2692: 1956 as the time for which a steady current equal to the prospective current would have to flow in a fuse to produce the same quantity of energy as would be produced if the actual current during the period of operation considered flowed in the fuse for the actual period. Values are derived from the simple formula

$$t_v = \frac{K_p}{I_0^2}$$

From Fig. 7 the r.m.s. value associated with the maximum admitted current for a given prospective current can be found. The method is as follows:

For a given prospective current the pre-arcing time can be taken from the full-line curve. From the 'virtual-time' curve the steady current or r.m.s. value of the pre-arcing current which has to flow in order to bring the fuse link to the point of arcing for this known time of operation can be read. This is, as it were, the r.m.s. value associated with the admitted current peaks taken from curve in Fig. 8. The alternative method of calculation is to make an appropriate allowance for the relationship between peak and r.m.s. value.

For times shorter than about 0·01 sec the curves show pre-arcing time, which, added to the arcing time, constitutes the total operating time. The arcing time is a complex function which is not mathematically determined, but is found empirically, usually to be 1–3 times the pre-arcing time, but not exceeding 0·006 sec. During the arcing time the current falls very rapidly, and is relatively negligible during the greater part of it. This is the principal reason why the curves are drawn to show the pre-arcing time. Since the arcing time does not exceed about 0·006 sec it is negligible in the lower part of the fusing curve.

(5) ACCURACY AND CONSISTENCY OF PERFORMANCE

The accuracy of the time/current and prospective-current/peak-let-through-current characteristics is dependent on the accuracy of manufacture of the element. The h.b.c. zones of the strip are blanked out to such fine limits that variations are insignificant, and the copper strip is rolled to very fine tolerances. The control of the time-lag insert is not so easy, but neither is it so critical, because many other factors contribute to time lag at this zone of operation over and above that contributed by the specific heat of the mass of the centre insert. All these factors are concerned with the rate at which heat is conducted away from the element and involve variations in the diameter of the powder grain, the inner diameter and thickness of the ceramic tubing, the dimension of the end cap and the amount of solder used in connecting the element, the length of the element between end caps and the moulded covering of boric acid flux. These manufacturing variations tend to a uniform average, and the result is a performance sufficiently consistent for all practical requirements.

Table 2

Comparison of Calculated and Test Performances of Fuses to C.S.A. Code 106 Form 1: Test Circuit at 600 Volts, 50 c/s, 0·114 Power Factor

Fuse	Current rating	Symmetrical prospective test current	Calculated pre-arcing constant	Pre-arcing time		Peak let-through current		Recorded arcing time	Recorded peak arc voltage*	Insulation resistance after test†
				Calculated	Recorded	Calculated	Recorded			
	amp	kA	amp²-sec	millisec	millisec	kA	kA	millisec	volts	MΩ
NAM	30	79·3	$1{\cdot}81 \times 10^3$	0·17	0·15	6·0	Illegible (~8·0)	0·55	930	48
NAS	60	79·2	$1{\cdot}04 \times 10^4$	0·31	0·26	10·9	Illegible (~10·0)	0·62	1010	80
NAI	100	80·0	$2{\cdot}89 \times 10^4$	0·42	0·37	14·7	12·0	0·71	950	350
NA2	200	79·2	$1{\cdot}81 \times 10^5$	0·78	0·82	27·3	22·0	1·54	1030	100
NA4	400	79·2	$7{\cdot}05 \times 10^5$	1·25	1·10	42·9	35·0	2·70	1060	22
NA6	600	79·2	$1{\cdot}63 \times 10^6$	1·70	1·50	56·9	47·0	3·50	1100	5

* Upper limit 2000 volts.
† Lower limit 0·1 megohm.

should be noted that the curves are produced starting with the fuse links cold—a condition rarely occurring in practice. The fuse-link element will, in general, have a running temperature depending on the loading, which will reduce the time lag provided on overloads open-circuited on the centre insert, since the running temperature will be a significant proportion of the melting temperature. When the h.b.c. zones are involved, however, the difference will be small, since the running temperature is low compared with the melting point of the copper strip.

(5.1) Tests

The previous Section covers accuracy of performance; attention will now be given to the problem of testing to show whether a design will retain its accuracy under all service conditions. So far, no attempt seems to have been made to ascertain whether deterioration has taken place on currents in excess of the rating but insufficiently heavy or sustained to rupture the fuse—a normal condition on motor starting, etc. In fact, B.S. 88 implies that a fuse link may deteriorate on currents between the fuse rating and the minimum rupture current.[5] To demonstrate non-deterioration in the design considered, tests were arranged to cover the following eventualities:

(a) The fuse link may be subjected to an overload less than the minimum rupture current but greater than the rating.
(b) The fuse link may repeatedly be called on to pass motor-starting overloads of many times the rating.

To cover these, three tests were specified to be made on the same fuse link:

Test 1.—For continuous heat run the fuse link is subjected to a current of 95% of its minimum fusing current, or 119% of its rated current in a Class P fuse link. This is maintained for 40 hours, and the cold resistance of the fuse is measured before and after the test.

Test 2.—The same fuse is then subjected to a current which would rupture the fuse in approximately 2 sec. This current is applied 12 times for 1 sec only, with intervals for cooling, and the cold resistance is measured before and after the test.

Test 3.—To demonstrate that tests 1 and 2 have not in any way affected the fusing factor or overload characteristics, the fuse is subjected to a current of 105% of the minimum rupture current or 131% of the rating in a Class P fuse. The time taken to rupture shall be in accordance with the published curves.

Fuse links of the dual-element design were subjected to these tests with satisfactory results. An extract from the test certificate in respect of the 100 amp rating is quoted.

Test 1.

Cold resistance of fuse before test	640 microhms
Heat-run current (being 125 amp − 5%)	118·7 amp
Length of heat run	43 h
(N.B. After 9 hours the voltage drop remained constant at 110·5 mV.)	
Cold resistance of fuse after test	640 microhms

Test 2.

The same fuse was then subjected to an overload of	600 amp
(Being six times full-load current.)	
Duration of overload	1 sec
This was repeated	11 times
(Making 12 overloads in all.)	
With intervals of 10 min for cooling between overloads.	
Cold resistance of fuse after test	640 microhms

Test 3.

The fuse was subjected to an overload of	131 amp
(Being 125 amp + 5%.)	
Time required to rupture at this current	2 h 5 min
Open-circuit resistance	> 100 megohms

Satisfactory co-ordination or discrimination between different ratings of fuse link in series can be accurately forecast by comparing pre-arcing constants. If that of the larger is more than twice that of the smaller, this is sufficient to allow for the energy circulated during the arcing period and discrimination will be obtained on all prospective currents, provided that the fuse links are of a similar consistent design and that the characteristics have not altered with service or time.

(6) ESTIMATION OF FAULT LIMITATION BY FUSE LINKS

Fig. 8 shows the relationship between the prospective current applied and the peak value of the current wave actually admitted. This gives an indication of the ratio of limitation of the instantaneous magnetic forces created in the circuit. To calculate the heating effect on the apparatus in series with the fuse it is necessary to know the r.m.s. value of the admitted current. Section 4 shows how this can be read off the curve given in Fig. 7. By using the portion in broken line indicating 'virtual pre-arcing time', the r.m.s. value for any given prospective current is obtained.

For this particular design of dual-element fuse a simpler method has been devised of estimating the degree of fault limitation by a fuse link. This gives the ratio between the power or energy actually admitted to the circuit and that which would have flowed had the protection been of the non-limiting type.

In this range of fuse link of uniform design the fault-power-limitation ratio and the energy-limitation ratio are related to the current rating of the fuse link, to the prospective fault current and to the time of operation. In order to express the degree of severity of a fault on a fuse link, the concept of 'severity factor' is introduced, showing the relation of the fuse-link rating to the prospective fault,

$$\text{Severity factor} = \frac{\text{Prospective fault current}}{\text{Fuse-link rating}}$$

The limitation of the fault power varies as the square of the limitation of the fault current, and is expressed by the ratio:

$$\text{Fault-power-limitation ratio} = \frac{(\text{Prospective fault current})^2}{(\text{R.M.S. admitted current})^2}$$

The relation between these is shown in Fig. 9.

As an example, a fuse link of 100 amp rating on a prospective fault condition of 10 kA has a severity factor of 100. From Fig. 9 the fault-power-limitation ratio is 7, i.e. the fault power is one-seventh of that which would have been passed by a non-limiting form of protection. At 50 kA the severity factor is 500 and the power-limitation ratio is 60.

The energy-limitation ratio is the product of the fault-power-limitation ratio and the ratio of the time taken for the fuse link to operate to that of the alternative form of protection. It has a bearing on calculating cable-heating effects and the energy released at the point of fault.

To calculate the energy-limitation ratio the total time of operation of fuse must be compared with an alternative form of protection. The total time of operation is the sum of the pre-arcing and arcing periods. It has been found that the effective arcing period may be taken as equal to the pre-arcing period for the purposes of the calculation. Fig. 7 gives the pre-arcing

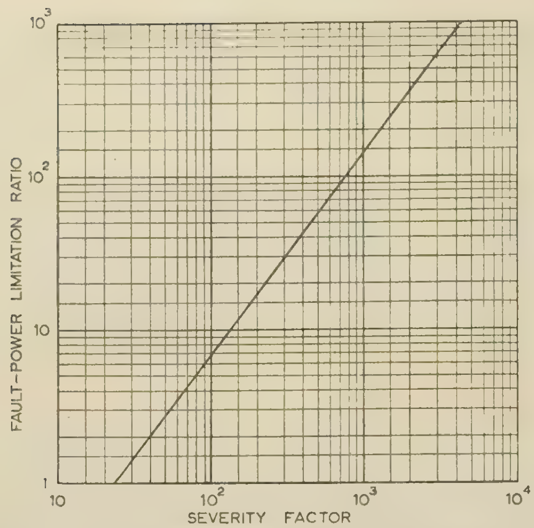

Fig. 9.—Curve for determining fault-power-limitation ratio.

time for varying prospective currents, and doubling this gives the effective total time of operation. The alternative form of protection may be a circuit-breaker, whose time of operation will not usually be less than one cycle, or 20 millisec on a 50 c/s supply.

The 100 amp fuse will then operate in 3·4 millisec on a 10 kA fault, and in 1·2 millisec on a 50 kA fault. The ratio of operating times are therefore 6·0/20 on a 10 kA fault and 2·4/20 on a 50 kA fault.

These time ratios, multiplied by the respective power-limitation ratios, give the energy-limitation ratios, so that the 100 amp fuse link would limit the energy circulated in the ratio 1 : 46 on a 10 kA fault and 1 : 1000 on a 50 kA fault.

(7) CONCLUSIONS

As to the future, the h.b.c. fuse link shows no sign of losing its popularity in Great Britain. Small and relatively inexpensive, it provides a form of protection which requires no attention and which limits the energy circulated under severe fault conditions. The scope for its application is widened by new forms of electrical apparatus. In North America the fuse link is again attracting attention after a period during which, because of the large sizes and the rather unsatisfactory electrical qualities of some of the local products, much development work has been carried out on small circuit-breakers in plastic cases. These have been widely used, but even on these there is now a trend to fit the larger sizes with back-up fuse links.

It has been stated that the design of fuse link considered will progressively limit the cut-off current faults exceeding 20 times the full load rating. The destructive power of faults varies as the product of the square of the current circulated and the time of fault duration. The limitation afforded by the use of fuses is particularly beneficial at points on the circuit of relatively high resistance and small heat capacity, such as contactors and isolating switches, which remain relatively undamaged on severe faults. It is also of great importance in limiting the damage at the location of the fault.

A direct measure of this capacity of a fuse link to limit fault current is its pre-arcing constant, K_p. From it the current at which the fuse link commences to provide limitation can be ascertained and the degree of limitation on larger currents can be calculated. It also leads to an assessment of the maximum thermal stresses to which a circuit protected by a fuse link can be subjected. For these reasons it is suggested that revisions of B.S. 88: 1952 and C.S.A. Code 106 could, with advantage, require pre-arcing constants to be quoted.

The author believes that the North American regulation which insists on a fusing ratio of 1·35, corresponding to the fusing factor of 1·25 of Class P of B.S. 88 for all general work, gives a greater degree of protection against fire risk than our arrangements in Britain, particularly for domestic installations.

(8) ACKNOWLEDGMENTS

The author is indebted to Parmiter, Hope and Sugden, Ltd., for the supply of the information on which the paper is based, and also for the constructive advice and help given by his colleague, Mr. E. W. Sugden, throughout the writing of the paper, and to Mr. G. E. Bowman for his co-operation in compiling data.

He would also like to acknowledge with thanks the assistance of Prof. F. M. Bruce.

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DISCUSSION ON THE ABOVE PAPER

Before the UTILIZATION SECTION *16th January*, 1958, *the* SOUTH-WEST SCOTLAND SUB-CENTRE *at* GLASGOW *6th November*, 1957, *the* NORTH-WESTERN UTILIZATION GROUP *at* MANCHESTER *14th January*, *the* NORTH MIDLAND UTILIZATION GROUP *at* LEEDS *21st January and the* NORTH-EASTERN CENTRE *at* NEWCASTLE UPON TYNE *27th January*, 1958.

Mr. J. W. Gibson (*at London*): The paper gives an interesting survey of British and Canadian requirements and describes a fuse which admirably satisfies both. A breaking capacity of 35 MVA is the highest so far specified in B.S. 88, while the Canadian specification calls for rather more than twice this, largely because Canadian practice tends to less system sectionalizing than British.

I cannot agree with the author's interpretation of the Canadian specification when he states in Section 1.1 that an object is to ensure adequate time lags; in fact, as in other North American specifications, emphasis is on the avoidance of excessive time lags with small over-currents. Nevertheless, I consider the time/current characteristic of the author's fuse to be excellent, in that close protection is given against over-currents and that it operates quickly on heavy faults, while being relatively slow at intermediate currents. Thus, for motor service, excessive over-fusing is avoided, while the contactor is well protected, not only against very severe faults, but also against damage through dropping out with lesser faults as the result of loss of coil voltage.

In practice, the risk of deterioration of fuses using M-effect is very small; they have been used successfully in millions for many years. The resistance/temperature coefficient of the element metal causes the temperature to change considerably with a small change of current as the melting point of the plug is approached;

thus the band of current within which deterioration can occur is very narrow. Since in the author's fuse the copper and the low-melting-point insert are soldered together, one might expect M-effect on overload. Is this so, or is the circuit broken by the melting of the insert as a whole before there is appreciable amalgamation with the copper?

Table 2 shows the fuses uniformly rather faster than predicted by calculation. Can the author say why? Was a similar discrepancy observed on the reduced-current tests of the Canadian specification? The insulation resistances, while all satisfactorily high, conform to no pattern in their variation. So far, there seems to be no adequate explanation for this very usual phenomenon with powder-filled fuses. What are the author's views?

The fault-power-limitation ratio can readily be shown to be equal to the pre-arcing time divided by the virtual pre-arcing time. Since Fig. 9 shows a single curve, the assumption must have been made that, at rated current, the current-density at the 'necks' is the same for all current ratings. This is presumably considered a reasonable approximation for the author's fuses, although it could not be applied to high-voltage fuses, where heat conduction from the elements is mainly radial, so that the current density falls considerably with increase of current rating. The slope of the curve is 4/3, which accords with the well-known fact that, for large prospective currents, the cut-off current varies as the cube root of the prospective current. However, for the lower fault-power-limitation ratios, the slope of the curve could be expected to diminish.

I agree with the proposal that fuse standards should require pre-arcing constants to be made available; this could be done either directly or by furnishing virtual-time curves, as in Fig. 7. The method of preparing such curves is already given in B.S. 2692: 1956 for h.v. fuses. The pre-arcing constant is then derived from the short-time end of the curve by multiplying time by the square of the corresponding current. Furthermore, I suggest that fuses be classified in terms of the current to cause melting in some such time as 5 sec.

Mr. H. W. Baxter (*at London*): It is gratifying that the author has succeeded in producing a range of fuses that give adequate protection without untimely blowing by harmless surges.

In support of the author's statement that maximum-breaking-capacity conditions are no longer the most onerous, I have tested an experimental fuse that cleared a prospective current of 40 kA satisfactorily but shattered when tested at 3 kA and at the same power factor.

The melting time is determined by a number of physical properties, namely resistance, mass, density, specific heat, thermal conductivity, thermal diffusivity and melting point. Has the author succeeded in confining the time-lag to the smaller over-currents, since to increase the lag with large over-currents reduces the ultimate breaking capacity?

With regard to Fig. 9, I query the assumption in regard to the hypothetical circuit-breaker operating in an arbitrarily chosen time. Circuit-breakers do not have zero impedance: a 100 amp air-break circuit-breaker can have an impedance of one milliohm or more, and this is sufficient to halve the power in a 600-volt 3-phase circuit of 80 kA prospective current. Moreover, a fuse operating with a small over-current might not operate quicker than a circuit-breaker of similar rating, so that the severity factor and fault-power-limitation would be no greater than unity. I therefore suggest that the lower end of the curve should pass through the point 1 : 1 and not 25 : 1 as at present shown. The user would probably be more interested in the value of $\int i^2 dt$, sometimes called the amp^2 seconds, passed by the fuse.

Mr. E. C. I. Macdonald (*at London*): This paper records something of a landmark, on two counts: a large oversea market for a British product has been studied, and to make sure of entering it successfully, an article has been produced which not only complies fully with the relevant regulations but—and this is the important point—also provides complete interchangeability with the local product. This example should not be ignored by industry in the United Kingdom.

The author's device for providing short-circuit zones in the fuse element by means of holes instead of necks and so reducing dimensions is interesting, but as a general point I think that persistent reduction of dimensions can be overdone, particularly in equipment which is used on systems with relatively high fault duties.

Clause 19 of B.S. 88 gives some guidance on the mounting of fuses for test, but the equipment in which fuses are subsequently used, particularly in distribution networks, introduces operating conditions very different from those under which the fuses are tested. Bad conditions for heat dissipation and dirt accumulation are by no means uncommon.

There appears to be no insistence that the minimum-fusing-current and duty tests shall be carried out on fuses at their working temperature. At present the fuse is allowed to cool to ambient temperature for these tests. Surely the internal condition of a fuse which has carried its rated current for two hours or more is much more sensitive to overload than it would be if the same overload were applied to a nearly cold fuse? Why should the fusing-current and duty tests not be applied to the fuse after it reaches a steady temperature due to a current flow of, say, 75% of its rating?

In rural areas the problem of medium- and low-voltage fuse protection differs somewhat from that of the protection of industrial plant, and in some respects the fuse must comply with rather conflicting requirements. Heavy loads must be handled, and the transient overload condition is much less severe than in industrial plant, or does not occur. The peak load is reached relatively slowly, and this, associated with a comparatively low available fault power, makes a low fusing factor desirable.

Mr. E. Jacks (*at London*): The paper is in two parts—a review of h.b.c. fuses generally and a description of a particular type of fuse in which the author is interested. I feel that the bias towards this particular design has led him to imply comparisons which do not reflect either current usage or current practice. The pros and cons of time-lag and quick-acting fuses have been familiar to engineers in this country for many years, and the latter have proved the more popular.

Since British manufacturers are able to meet the higher rupturing capacity required in Canada, it is time that British Standards recognized the fact. In this respect B.S. 88 has lagged behind world opinion. Hitherto, it has been considered good practice to sectionalize systems and so keep fault levels down. This is sound engineering so long as it remains economical, but the use of h.r.c. fuses with higher rupturing capacities can save money, and should be seriously studied from this aspect.

Finally, although the author mentions that the Canadian specification has been framed around British experience, the general impression given is that North American requirements and practice are ahead of those in this country. One has only to remember that the Canadian specification for h.r.c. fuses made its only appearance 5 years ago and that the United States as yet has no specification at all for them to realize that this country still maintains a considerable lead.

Mr. E. W. Sugden (*at London*): The first widely successful h.b.c. fuses were introduced in the early 1920's by the late Vernon Hope and were recommended by Grant* in 1926 for breaking capacities of 100 MVA at 600 volts. They exploited two essential features, both of which are retained in the designs

* GRANT, L. C.: 'High-Power Fusible Cut-Outs', *Journal I.E.E.*, 1926, 64, p. 920.

reviewed by the author, namely deliberate energy limitation by current cut-off and the provision of a low fusing factor, so ensuring close protection of the circuit, the use of available materials with safety, and the possibility of freedom from deterioration under all likely conditions of load and overload. I think that the possibility of achieving the latter has been generally underestimated, so that Hope has not been followed in respect of low fusing factor as he has in the provision of controlled pre-arcing constant, which is now a feature of all successful h.b.c. fuses.

To some extent a low fusing factor was especially essential to the Hope fuse, on account of his use of fibre tubes; but although ceramics are now used, the dependence of a non-ageing characteristic on a low fusing factor has been realized over the years, and this feature is retained.

The use of copper instead of silver in the fuse element helps towards the achievement of the same end, because copper is relatively insensitive to the M-effect. A dual-purpose element, in which the circuit contains a discontinuity consisting of low-melting-point metal to fuse promptly at or below the temperature at which the deterioration of conductivity is initiated by M-effect, is far easier to provide with copper. I do not wish to belittle in any way the use and value of silver fuse elements, nor, indeed, to criticize the use of M-effect to lower the minimum fusing current and secure a blow at a temperature nearer to 250° than 950°. I believe that the use of M-effect is the only effective alternative to the dual-purpose element yet discovered, and this, I believe, is by far the most important reason for the widespread use of silver as the element metal.

[Similar points were raised by several other speakers.]

Mr. W. J. Elliott (*at London*): It is difficult to appreciate the advantages of a dual-element fuse of the characteristics shown in Fig. 7 when used for the application shown in Fig. 2. The whole of curve (*c*) in Fig. 2 would appear to lie in the time-lag zone of the fuse characteristic, and unless the change from h.b.c. zone to time-lag zone can be brought much closer to the take-over point of curves (*c*) and (*b*), the let-through current on short-circuit will be much higher than it would with a simple h.b.c. element [see curve (iv), Fig. A].

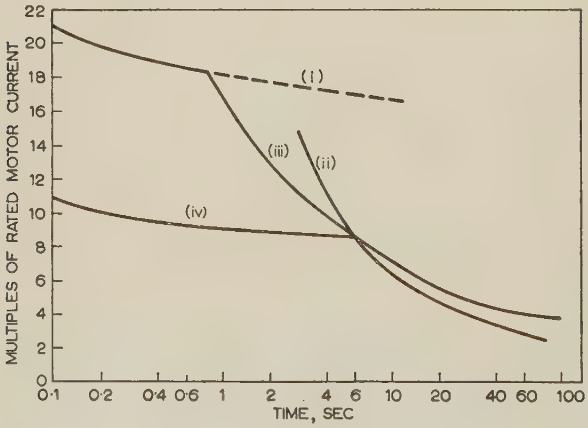

Fig. A

Again, the take-over point cannot be determined as a definite current. Both curves (ii) and (iii) are subject to variation from the ideal. Fig. B shows these curves replotted with tolerances on currents of ±5% for the fuse and ±10% for the trip, and indicates that with a given design the fuse may blow at as low a value as 6¼ times full load, but the starter must be capable of breaking more than 10 times full load.

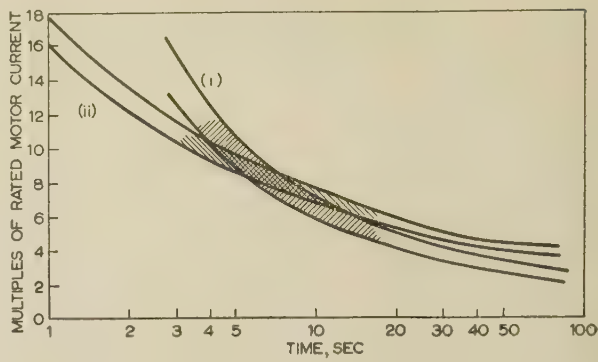

Fig. B

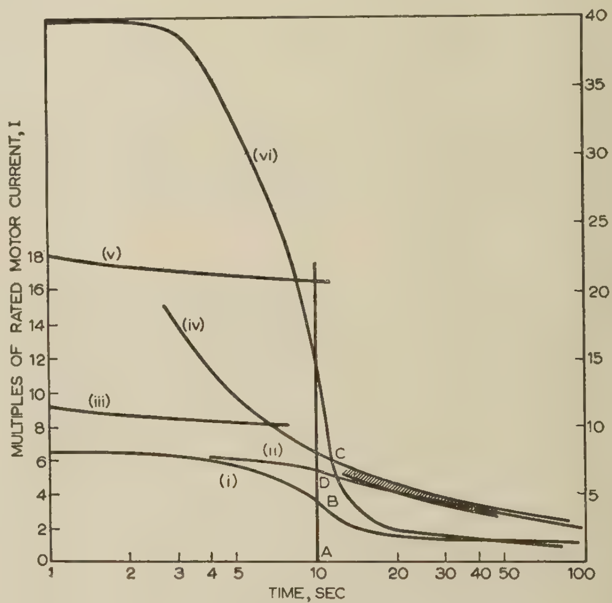

Fig. C

Furthermore, selection of an overload setting cannot be made by direct comparison of a trip curve with a motor-current curve. In Fig. C, AC is the steady current which would trip the starter in 10 sec, while AB is an instantaneous motor current and less than that which has been flowing up to that time. The r.m.s. value, AD of curve (i) between 0 and 10 sec, is the steady current equivalent to the varying motor current over that time, and curve (ii) shows the progressive variation of this equivalent with time. This curve actually cuts the lower limit of envelope (iv) and so the starter may trip. This will result in derating the trip and uprating the fuse, so that the 1·25 factor of fusing current to full load cannot be maintained. Not only would there appear to be little advantage therefore in using a dual-element fuse, but a simple h.b.c. element [curve (iii)] would give lower cut-off for the same take-over current.

[Similar points were raised by several other speakers.]

Mr. C. G. Whibley (*at London*): From Section 6, I conclude that the severity factor and the fault-power limitation ratio have no practical value except to make Fig. 9. The important thing required in the fuse world is not factors which must be converted back to actual units for the necessary calculations, but expressions which are simple and direct. In that respect I appeal for two quantities, namely the peak cut-off current and the total

amp²-seconds during fault clearance. The former will provide data for the design of the associated electrical contacts and conductor support to meet initial thermal and electromagnetic forces, and the latter will give a value from which the energy released in the circuit can be calculated to evaluate the thermal rating of the conductors and the fire risk at the point of fault. These direct quantities can be obtained from the oscillograph records which are essential for the proof of performance either to Canadian or to British requirements. I emphasize the need for these simple and direct figures because I agree with previous speakers that the h.b.c. fuse has a very definite place in our electrical industry.

Mr. J. A. Robbins (*at London*): The suggestion of using fusing factors of about 1·3 would seem to give a desirable improvement in fire-risk protection, particularly since some cable current ratings have been increased since the effective completion of Gosland's investigation. If, however, fusing factors of 2 are considered to give adequate protection against cable overloading, I would support a suggestion that The Institution's Wiring Regulations may with advantage be modified to permit using smaller cables when they are protected by devices having fusing factors of about 1·3. One major point relevant to fuses with these low fusing factors is that this performance must not be achieved at the expense of unwanted operation on such transients as motor starting currents, otherwise this development will almost inevitably result in overfusing troubles.

In Section 13(*c*), circuit-breakers are classed as non-limiting protection, but this is not necessarily true. The impedance of even 100 amp circuit-breakers can give a degree of current limitation. Certainly the internal impedance of lower current ratings of miniature circuit-breakers produces very definite current limitation. Current chopping can also take place, which again is effectively current limitation.

The circuit-breaker operating time of 1 cycle quoted in Section 6 seems high: usually the overall operating time of miniature circuit-breakers under such conditions is about ½ cycle (10 millisec).

I should like to clarify the comment in Section 7 regarding miniature circuit-breakers with back-up fuse links. American 'moulded case' circuit-breakers may be rated up to about 600 amp, and only for these larger units has interest been shown in using back-up fuse links. Experience has shown that, on normal installations, high breaking capacities are not really required with ratings of 60 amp or less. Consideration is really being given to using back-up fuses only on ratings above about 100 amp, thereby retaining the operational convenience of circuit-breakers despite rather higher prospective fault currents.

Dr. H. F. Maass (*at London*): This design of dual-element fuse presents in effect two fuses in series: the short-circuit zones and the centre time-lag insert (Fig. 6). It is therefore essential to show that the performance of both is satisfactory individually and that they will operate together without difficulty. The British and Canadian Standards cover the duty of fuses by breaking-capacity tests at maximum prospective current and, for current-limiting fuses, at a lower current at which severe arc-energy conditions obtain. Satisfactory performance of these tests by the fuse described proves only the performance of the short-circuit zones; the centre insert does not come into operation, and further tests at the 1–2 sec fusing current appear indicated to show that this part of the fuse is capable of breaking the circuit at full voltage. Have such tests been made?

The tests for non-deterioration (Section 5.1) check the centre insert only and not the short-circuit zones—the reverse position to that just described. The short-circuit zones would be severely stressed in service without actually blowing, in case of back-up protection for a lower-rating fuse. They are of tinned copper, which is more likely to deteriorate than silver.* Also the fact, apparent from Table 2, that the pre-arcing time is somewhat less than calculated may point to such deterioration.

With regard to co-operation of the two parts of the fuse, an overlap in the take-over region is needed, and some evidence of its existence is desired.

Mr. D. B. Hogg (*at London*): As a user of fuses in a large-scale industry, my experience has been that, when the types comprising a large number of small silver-wire elements in parallel are used to protect large squirrel-cage motors with direct-on-line starting, they are apt to deteriorate, whereas no deterioration was noticed in many hundreds of such cases using the author's fuses. The reason for this may be as follows: in the multi-wire-element fuse, because three fuses are required, the heavy magnetic forces with the very large starting currents drive the current to the few outer elements in the two outer fuses, so that these elements, instead of carrying 6–8 times the full load at starting, may be carrying 60–80 times full-load current per element, and it is thought that one or more will melt, owing to the M-effect. If this happens, of course, there are fewer wires for the next time and deterioration will be rapid, so that the fuse may melt below normal full-load current. With the author's fuses, on the other hand, which consist of two thin copper foils in parallel and a large alloy pocket in the middle, even if the starting current is driven into one of the foils, thermal conduction will tend to keep the temperature down.

In the earlier and somewhat smaller forms of these fuses, when the cases were made of organic materials, the copper strips were brought out through the ends and became part of the contact, ensuring that the strips in different fuses were parallel to one another and thus making the conditions much easier. With the new ceramic pot fuses the strips may be at any angle to one another, but the change is not thought to be material. Has the author been able to carry out any tests which would have a bearing on the above suggestions?

Mr. G. N. Harding (*at London*): Are the author's fuses capable of dealing with the surge currents caused by the use of C-core transformers, where the initial peak may rise to the order of 30–40 times the normal operating current of the transformer but protection is required against steady overloads of the order of 2–3 times the normal current?

Secondly, will the fuses, which use a tin-lead alloy, withstand conditions of vibration and shock?

Thirdly, is it possible to design a fuse to meet such conditions when operating in ambient temperatures in excess of 200° C?

Messrs. B. C. Hicks and **K. Dannenberg** (*communicated*): In Section 2 the author states that there are only two practical solutions to the problem of reducing temperature rise under overload conditions with a low fusing factor, namely the M-effect or a time-lag device; but we would point to other methods, both chemical and mechanical, for achieving this. Some years ago, Continental literature described the use of alkali halides attached to the silver element. These salts are extremely stable, but on melting due to the temperature of the conducting element on overload, a liquid containing free halide ions is formed. This quickly reacts with the silver element, increasing its resistance and causing the fuse to operate. Another method is the use of flash powder, which, when ignited, open-circuits a percentage of the conducting elements. This achieves low cut-off currents, in contrast to the author's fuse.

Test evidence relative to the h.b.c. fuse described should be provided, in order to prove that the fuse will interrupt satisfactorily when the thermal device on its own is subject to a maximum current condition about 7 times the fuse rating.

The author suggests in Section 5 that his deterioration tests at

* Baxter, H. W.: 'Electric Fuses' (Arnold, London, 1950), pp. 25, 26 and 31.

over-currents below minimum fusing are unique; but some, and probably all, manufacturers have done similar tests. Deterioration would likely occur within the tinned-copper short-circuit zones of the dual element. The author's deterioration check concentrates on the condition of the thermal device with a minimum fusing-current test.

There are prospective currents where sufficient energy, both from the adjacent short-circuit zones and as direct-current heating, passes into the thermal device, causing volatilization of the boric oxide and therefore an increased critical stress on the fuse ceramic. This could occur at currents of 10–15 times the fuse rating, and test evidence of the fuse link in this region would appear to be essential.

Mr. T. B. Rolls (*communicated*): For at least 25 years the term 'h.r.c. (high rupturing capacity) fuse' has been widely used in this country. B.S. 88: 1952 (Appendix A, Section 6) condemns this expression, first as being vague because 'high' is merely relative, and secondly, because B.S. 205 substitutes 'breaking capacity'. At the best 'breaking capacity' must be carefully related to the prospective fault current before it has any real meaning when applied to a fuse. Must we now start the term 'h.b.c. (high breaking capacity) fuse', which is accepted by neither B.S. 88 nor B.S. 205? If we must alter our nomenclature, would it not be better to drop 'h.r.c.' and 'h.b.c.' entirely and refer simply to 'cartridge' fuse links and, where greater precision is needed, refer to B.S. 88?

Mr. J. K. Wheeldon (*at Glasgow*): The current limit of 80 kA specified in C.S.A. 106 is higher than that in B.S. 88. This seems to encourage system engineers to lay out systems with very high short-circuit currents, which is surely the wrong policy.

It is not essential for all fuse links for Canada to have Form I overhead characteristics, and Form II fuses which are equivalent to the British Standard Class Q fuse have many applications. In Section 2 the author states that simple elements of silver and copper are not suitable for the production of cartridge fuse-links

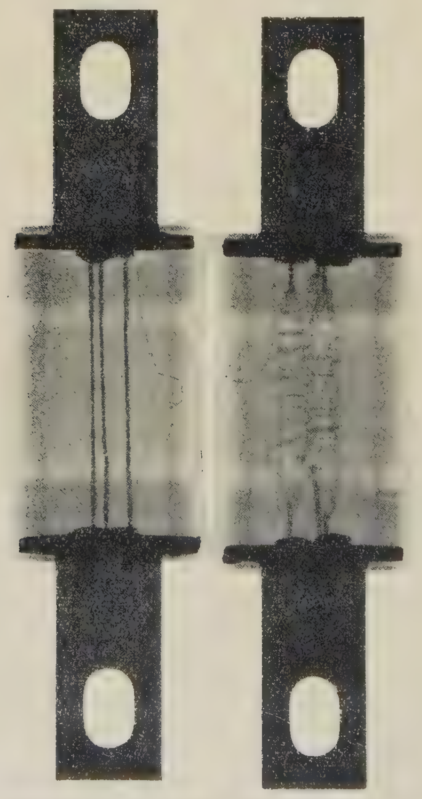

Fig. D.—Radiographs of blown and unblown fuse elements.

fulfilling all requirements. This is incorrect: Fig. D shows a radiograph of an unblown fuse and a fuse blown at 440 volts AC5 rating. Simplicity of design and the use of silver in the centre of the fuse link make for reliability, and this can certainly be claimed for the design shown, which meets the full requirements of B.S. 88.

Mr. J. E. Sayers (*at Glasgow*): My main interest lies in protection of final sub-circuits for motors. Ratings of a.c. switching starters covered by B.S. 587 assume starting currents not exceeding 8 times full-load current up to 100 h.p. and 6 times above 100 h.p. Therefore the cross-over point between curves (*b*) and (*c*) in Fig. 2 should be not higher than these values, depending on the size of motor protected, otherwise the motor starter may have to break currents substantially exceeding its rating.

The author indicates that the dual-element fuse link can be made to operate at a fusing factor as low as 1·1 with a long time-lag for motor-circuit protection. This is invaluable for starting high-inertia drives, such as fan impellers, where starting times of 25–60 sec or more are common. With such drives the period of high starting current shown in Fig. 2 must be greatly extended without running into operation of the fuse or overload protection. Thermal overloads will meet these conditions, but the fusing of motor circuits from a distribution board is a difficult problem. It is a poor solution to overcome this difficulty by the all too common practice of using a fuse of high fusing factor and high rating, with consequent repercussions on protection of circuit cables or even the need for larger cables than are otherwise warranted. On this point also, I would question Fig. 2, which indicates a fuse rating at least three times greater than the normal motor current.

I take it that the author's point in his concluding paragraph is that the low fusing factors used in America (1·35), or obtainable with the fuses he has described (1·25), give greater protection than the ratios of about 1·7–2·0 more commonly employed in this country. Surely this contention cannot be disputed.

Mr. W. H. Howard (*at Glasgow*): Its inverse-square current/time characteristic gives the h.b.c. fuse link a speed of operation which cannot be matched by any other device at such low cost, and provides a unique measure of protection against thermal shock and open flashing in lightly conductored installations. Whereas a correctly chosen h.b.c. fuse link can generally provide complete short-circuit protection at system fault levels up to its proved breaking capacity, the much greater clearance time of a similarly rated circuit-breaker may result in permanent damage and much greater fire risk in a lightly wired installation.

Public network fault levels will continue to rise as the industry develops. Attempts to hold systems to low, arbitrarily chosen, fault levels lead in the long run to uneconomic designs, and I feel that this is not sufficiently appreciated by system designers. For an optimized low-voltage network design, for example, a load growth of four times can best be met by twice the number of transformer stations, each station having twice the rating; and unless transformer impedances are also increased or costly subdivision of capacity is resorted to, a substantial increase in fault level is the result. A further point to be taken into account is the substantial increase during recent years in the use of voltage-sensitive appliances, including electronic equipments. During the same period there has been an increase in the use of plant of the rougher kinds, and the sizes of individual units are continually increasing. If these trends continue, stronger supply networks will become necessary. For these reason I believe much greater use will be made of h.b.c. fuse links in order to obtain acceptable standards of safety in consumers' installations.

Mr. C. Ayers (*at Manchester*): It is of interest that in various specifications the fuse is recognized as a specially designed piece of electrical equipment, although its characteristics, apart from

the highest breaking current and the minimum fusing current or—in certain specifications—two low currents, are not defined. In fact, phrases such as 'a given current for a sufficient time' are common. Such loose definitions are not in the best interest of further development of a specially designed piece of equipment.

There are two regions of operation of particular interest which involve different considerations of circuit design, namely

(*a*) The high-current region in which the fuse element ruptures in less than half a cycle, exhibiting cut-off, and where discrimination with other fuses is of prime importance.

(*b*) The low-current region in which the fuse ruptures in times varying from half a cycle up to possibly hours, where co-ordination with other forms of protection is a major consideration.

In the high-current region I agree with the author that one of the major parameters of interest is the pre-arcing constant coupled with the concept of virtual time. For positive discrimination between major and minor fuses the author states that the pre-arcing constant of the major fuse should be twice that of the minor. Other published data indicate that the ratio, based on rated current, should be 2 : 1 or 3 : 1, depending on the prospective current but more significantly on the manufacturer of the fuse. Such a simple relationship applied only to a consistent range of fuses designed in a similar manner, not to fuses of different design or manufacture. Surely the time has now arrived when standardization would be of benefit, and I suggest that manufacturers present information regarding the heat integrals at the critical points of operation.

Mr. E. Jacks (*at Manchester*): In view of the similarity of the design in Fig. 4(*a*) to one in current use it should be stated that M-effect need not be involved in such an element. All that is required is to proportion the element so that the arc is initiated at the centre of the gap, thus leaving the joints unaffected when the fuse is interrupting overload currents. M-effect can be useful when properly controlled, but it should not always be considered a necessary adjunct to obtaining desirable time/current characteristics.

The author's reference to energy limitation rightly draws attention to one of the more important attributes of h.b.c. fuses, and although the introduction of new terms must proceed with caution, the suggestion made will form a useful basis for further study.

Dr. H. F. Maass (*at Manchester*): I do not agree that the two values of maximum blowing time specified for Canadian Form I effectively govern the time/current characteristics of a fuse and assure adequate time-lag. The specified times are relevant to sustained overloads only, and time-lag performance is not controlled. The effect of B.S. 88: 1952 is similar; it is also based on two tests: blowing and non-blowing currents at a stated time. Besides this provision, B.S. 88 requires manufacturers to produce time/current characteristics, and I favour a standard form of double-logarithmic graph paper for these with a ratio of cycles of 2 : 1, as in Fig. 7, but with the time and current axes interchanged. The author has a preference for fuses with low minimum fusing currents. Such fuses no doubt have their field of application but are hardly desirable as general-purpose units. Considering only one important application, the author's case is considerably weakened by his statement in Section 1.3(*a*) that the fuse links in question will have thermal overload protection. It seems that the fuse is encumbered unnecessarily by an arrangement giving close control in a current range where the motor is protected by the thermal relay and which is below the prospective fault current of the cable connecting fuse and relay.

Mr. A. L. Lawrence (*at Manchester*): To prove non-deterioration the author deals in Section 5.1 with pre-arcing times longer than 1 sec; but if non-deterioration is to be proved for all currents which do not cause the fuse to blow, then surely it must also be proved for pre-arcing times where the h.b.c. zones of the element are to operate and not only for the centre insert. Two eventualities have been given, but is there not a further case where two fuses in series are required to discriminate at all currents? For short pre-arcing times of the minor fuse, the major fuse may have the h.b.c. zones sufficiently stressed to cause deterioration. Should not this third eventuality be considered?

For the very long operating times, test 1 is suggested at a current 5% below the minimum fusing current. This is very useful, and it is of interest to note that it is similar, but with a longer duration, to part of the minimum fusing test in B.S. 88: 1952. Test 2 is equivalent to testing the 100 amp fuse considered at 75% of the 1 sec fusing current given in Fig. 7. In view of the 95% value in test 1, would it not be possible to increase the 75% value used in this test? I appreciate that somewhat more elaborate test equipment might be required, but I do know of one Continental manufacturer who uses a test of 90% for another purpose.

For pre-arcing times shorter than 1 sec no tests are suggested. Should not a further test covering the eventuality already mentioned be made at the rated voltage, by testing two fuses in series? With a 100 amp and a 60 amp fuse in series, the 60 amp fuse would operate leaving the 100 amp fuse intact, which could be checked for deterioration. With this further test, non-deterioration can be considered up to the breaking capacity of the fuse and not restricted to times above 1 sec, as given in the paper.

Mr. C. A. M. Thornton (*at Manchester*): When B.S. 88 is revised I suggest that the categories of duty should be modified by increasing the prospective current for AC1 and AC2 duties and by adding 660 and 750 volts to the list of alternating and direct voltage ratings, the latter for the Continental market. I would not suggest a prospective current higher than 46 kA unless the American market demands it. As many as possible of the categories of duty at the various voltages should have the leading dimensions of the cartridge fuse links standardized, and physical dimensions should be a minimum consistent with satisfactory performance on test. Complete fuses should also be units of minimum physical size. I prefer the contacts between the fuse link and carrier contact, between the carrier and base contacts and between the base contact and the cable to be independent. The maximum temperature rise at rated load anywhere on the surface of a cartridge fuse link should be specified, and there should be provision for non-explosive indicators. A test finger should be introduced for testing danger when the carrier is partially and completely removed from the base.

Attention should be given to the requirements of cartridge fuse links in industrial plugs for use with ring-main-connected sockets, ultimately up to 200 amp rating and for single-phase, 3-phase and d.c. use up to 750 volts. These will either require compact cartridges mounted in the plug top or preferably fuse pins, particularly up to 63 amp. Pin diameters envisaged for the new international plug standard in course of preparation by the C.E.E. are 5, 6 and 9 mm for the 16, 32 and 63 amp ratings respectively, and the pin volumes envisaged are respectively $0\cdot5$, $1\cdot1$ and $3\cdot0\,\mathrm{cm}^3$. To bring fuse-link cartridges within these dimensions involves development beyond B.S. 1361 or 1362, and I suggest that the maximum category of duty that can be accommodated in this pin space at various voltages should be investigated.

Mr. A. J. Coveney (*at Leeds*): Fuses can obviously be made with fuse factors varying from $1\cdot3$ to $1\cdot9$, and it is usually accepted that, if a fuse is essentially required as a fault-clearing device, the low fusing factor is not essential. If the author has, as he says, produced a design which can meet both fault-clearing

and overload requirements, he has obviously achieved something worth while.

Industry is to-day considering the testing and certification of fuse links as distinct from fuses. It should be quite clearly stated that if a fuse is tested, then it means the whole unit, i.e. the carrier, base and contacts, as well as the fuse links. Results here can be very different from those of tests on fuse links only. It is noted that in test 2, Section 5.1, the author has made a test for half the time which is considered necessary to blow the fuse. I should have thought that, to prove there was no deterioration, he would have been prepared to make the test more nearly approach the fusing time.

It is now universally recognized that the modern cartridge fuse, with its short clearance time, is eminently suitable for short-circuit clearances, and this feature has led to a tremendous increase in their use in place of circuit-breakers. There is a danger, however, in the making of a circuit under possible fault conditions. It can be a very difficult and slow operation to insert a heavy current fuse link with its carrier by hand into its base contacts. There have been cases of injury to personnel carrying out this operation when fuses were being replaced on circuits left with faults on them. In my opinion, cartridge fuses must be used in conjunction with a fault-making and through-fault-carrying air-break switch, and so interlocked that the fuse is in position before the switch can be closed.

Mr. B. Summerville (*at Leeds*): How does the fuse link developed for the North American market differ from that which the author's company has been manufacturing for many years?

Mr. F. Clarke (*at Leeds: communicated*): The three tests specified in Section 5.1 should do much to allay any fears regarding the possible premature fusing of h.b.c. fuse links, but the paper does not make the position clear regarding these tests. How widespread are such tests and are they carried out on fuses for the British market?

I agree with the author that springs and other moving devices should be avoided, but would like to have confirmation that the dual-element strip shown in Fig. 6 does not suffer from the defect usually associated with M-type elements, i.e. erratic operation at currents between full-load and minimum breaking current.

Mr. R. H. Dean (*in reply*): The discussion, apart from minor details, has centred on two main points, namely the operation of the fuse link on large prospective currents and the performance when time lag is required in relation to motor starting currents, with the reference to regulations concerning the relation between the fuse-link rating and the cable ratings.

H.B.C. Fuse Performance.—The reason why, in Table 2, the fuses are uniformly faster than predicted by calculation, as pointed out by Mr. Gibson, is because a heat-dispersion factor was included in the calculation. Had we assumed no heat loss, the test and calculated figures would have been almost identical. Again, referring to Table 2, the insulation resistances vary in a manner for which, at the moment, we do not know the reason, although they well exceed requirements. I believe that the M-effect does not operate to a significant extent with copper and thus with this design of fuse. Several contributors have rightly commented on the joint between the copper strips and the centre tin-lead-alloy plug. This joint is designed so that it does not melt on any overload condition and so is not a zone of operation of the fuse, but transfers the heat from the h.b.c. perforations through as wide an area and with as low a temperature gradient as possible.

Mr. Baxter asks whether the centre time-lag zone affects operation on large currents. The centre portion operates on currents of up to approximately ten times the rating of the fuse link. Up to 15 times the rating the presence of the centre portion increases the time lag, although blowing on the h.b.c. perforations; but above this current, the amount of heat transferred is so small that it has no effect. In preparing Fig. 9 the resistance of certain types of circuit-breaker was not included so that it would in part compensate for other factors, such as asymmetry. The main purpose of this Figure was to demonstrate that the reduction of power and energy under fault conditions by the modern h.b.c. fuse link is of such an order that it could be taken to be axiomatic that a circuit protected by the modern h.b.c. fuse cannot be damaged by fault current of any magnitude.

Motor Starting-Time Lags.—Referring now to the operation under motor starting conditions, Mr. Elliott, Dr. Maass, Mr. Wheeldon and Mr. Ayers have made valuable contributions, but in every case they have regarded the fuse link as being incorporated in the apparatus it is protecting, i.e. for back-up protection. It is not the object of the paper to examine the design of fuse links for special purposes. The fuse link normally used must, by regulation, protect the circuit cable. In this context the points discussed can be grouped under the following headings:

(*a*) Is it desirable that a fuse link should operate on a minimum fusing factor of 1·25 (Class P, B.S. 88)?
(*b*) Can a fuse link of a 1·25 fusing factor have sufficient time lags to permit the passing of temporary overloads such as motor starting?
(*c*) What are the regulations concerning the relationship between fuse-link and cable ratings?

The advantage of low fusing factor—a requirement on the North American continent—lies in the capacity to blow at the lowest current consistent with its rating. Where earth continuity of low resistance is difficult to obtain and maintain there is risk that a phase-to-earth fault will not blow the fuse link. The fuse link which operates on a 25% overload is much safer than a 75% (Class Q, B.S. 88) type. Again, a fault current of up to 25% will not overstress the cables, but a 75% overload will. This also applies to ageing installations, where insulation resistance is falling.

A dual-element fuse has time lags such that the rating of the fuse link need not be much increased to accommodate the starting currents, even of direct-started motors. Fuse links of simple silver wire or strip design have little time lag, and generally have to have a rating of 2–3 times the full-load current of the motor. The degree of protection that is thus lost is further magnified by their fusing factor, which is usually 1·75. Table A illustrates the degree of overwiring.

Regulations on the relationship between fuse link and cable ratings recognize a danger, but when specifying a fuse-link

Table A

Manufacturer's Recommended Rating for Direct Starting

Motor full-load current	Power at 440 volts 3 phase	Fuse-link rating		
		Dual-element fuse	Silver-element fuse	
			Design A	Design B
amp	h.p.	amp	amp	amp
7·5	5	15	25	25
19	15	30	60	60
39	30	50	100	100
63	50	80	200	150
95	75	125	200	250

Table B

EFFECT OF FUSE TYPE ON RATING AND AREA OF PAPER-INSULATED CABLE

Direct-started 50 h.p. motor drawing full-load current of 63 amp

Dual-element fuse				Silver-element fuse					
Fuse		Cable		Fuse		I.E.E. cable		Lloyd's cable	
Rating	Minimum blowing current	Area	Rating	Rating	Minimum blowing current	Area	Rating	Area	Rating
amp	amp	in.^{2}	amp	amp	amp	in^2	amp	in^2	amp
80	100	0·0225	83	200	350	0·04	120	0·06	150

rating make no reference to classification. The Institution's Wiring Regulations (Rule 316) require that the rating of the fuse link shall not exceed that of the cable, except in a special case, where it may be double. Lloyd's have the same rule (365) for marine work, the usual easement being 50%. How the dual-element principle reduces cable sizes is shown by Table B.

The minimum fusing current of the respective fuses is quoted, together with the current rating of the cable, so that the degree of protection by the two types of fuse can be compared.

Great interest is shown in the reliability or non-deteriorating tests. Mr. Lawrence is correct in pointing out that, while these cover continuous loads of up to 5% lower than the minimum blowing current and surges of six times the rating, we do not go on to demonstrate the non-deteriorating capacity of these fuses when they are operating on the h.b.c. zones. There is no theoretical reason why this should not be done, but why it should be necessary is not clear. The fuse link of these large currents is operating in a manner similar to all other h.b.c. fuse links; furthermore, the chances of a temporary surge above 10 times the rating of the fuse link are very remote. The current surges which occur every time the motor starts, and overload conditions, are surely of major interest and the real test of a non-deteriorating fuse link. These tests—in reply to Mr. Clarke—are applicable to fuse links either to Canadian or British standards and are novel. No other design of fuse seems to have deterioration tests in excess of their full-load ratings published, nor were any offered during the discussions—a fact I regret. The experience of Mr. Hogg, as a large user, regarding non-deterioration during many years of service in this type of fuse is particularly significant, since this gives final confirmation.

Mr. Harding's requirements call for a rather special type of fuse which is outside the scope of the paper.

Space does not allow more than an acknowledgment and appreciation of contributors not mentioned, particularly to Messrs. Thornton, Sayers, Howard and Macdonald. The points they raise are of great interest, but lead away from the specific subject, and lack of space does not permit my dealing with them individually.

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LABORATORY AND FIELD TESTS ON 132 kV SYNTHETIC-RESIN BONDED-PAPER CONDENSER BUSHINGS

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(*The paper was first received* 18*th March, in revised form* 12*th September, and in final form* 19*th October*, 1957. *It was published in January*, 1958, *and was read before the* SUPPLY SECTION 29*th January*, 1958.)

SUMMARY

Measurements of the properties of a number of the condenser parts of 132 kV bushings which have been in service for 20–25 years have been made in the laboratory. Destructive examination of bushings with characteristics typical of clearly defined groups has been carried out. This has enabled particular power-factor/voltage and capacitance characteristics to be associated with definite defects, provided that either the original characteristics are known or a sufficiently large sample of bushings of the particular type has been tested to permit these properties to be inferred.

The measurements have been extended to the field, the various difficulties encountered being discussed. It is shown that the properties of the other bushing components and fittings such as porcelain weathershed, arc shield, oil and breaker parts affect the measured properties to the extent that minor defects cannot be diagnosed as they can be on tests carried out on the condensers alone in the laboratory. A technique which permits the detection of major defects by non-destructive tests on installed bushings is described. These methods have been applied to over 1 600 132 kV oil-circuit-breaker bushings, with the result that approximately 4% defective bushings have been removed from service and another 3% classified as being in doubtful condition.

(1) INTRODUCTION

Many 132 kV condenser bushings have been in continuous operation on the British Grid system for 25 years or more. These bushings, whose main insulation is synthetic-resin bonded paper (s.r.b.p.), have, in general, given excellent service, but there have been occasional failures. Since an insulation failure may cause injury to personnel and damage to equipment, apart from the inconvenience of loss of supply, it is essential that the insulating properties of these old bushings should be checked, to prevent breakdowns in service.

As a result, an intensive investigation was undertaken, initially in the laboratory with the object of establishing reference test methods, and then in the field to supplement the normal routine measurements which are made regularly.

Existing non-destructive methods of testing bushings consist of measurement of

(*a*) Insulation resistance at a relatively low direct voltage.[1]
(*b*) Power factor and capacitance at service frequency, measured by bridge or wattmeter.[1,2,3,4]
(*c*) Voltage distribution on the outside of porcelain weathersheds.[1]
(*d*) Leakage current at high direct voltages.[5]
(*e*) Electrical discharge properties.[6]
(*f*) Dielectric dispersion.[6]

In most cases these measurements give information concerning one particular property or one aspect of deterioration only. For example, insulation resistance measurements will detect a common cause—ingress of moisture—but it has been found that, for detailed laboratory investigations, power-factor and capacitance measurements carried out over a voltage range are capable of yielding information which defines the bushing condition comprehensively.[2,7,8,9] For this reason, the power-factor/voltage test has been used as the main laboratory reference method, and this paper deals first with the interpretation of the results obtained in the laboratory and their significance.

Secondly, field testing is discussed, and the results are summarized of tests on 1 600 132 kV bulk-oil circuit-breaker bushings of the condenser type with oil filling, which have been in service for 20–25 years.

It is the object of the paper to present the results of this work and describe the mechanisms of deterioration observed in the bushings, the principles established being applicable to all condenser bushings.

Mr. Douglas and Mr. Stannett are at the Central Electrical Research Laboratories.

(2) LABORATORY TESTS

(2.1) Description of Bushings Examined

The bushings discussed in Section 2.2 are all of the same overall dimensions but were manufactured by four different sub-contractors, A, B, C and D, during 1929–31. A typical design is shown in Fig. 1, the capacitance being 224 pF and the capacitance per section varying between 2 000 and 5 600 pF, and consequently supporting voltages between 8·7 and 3·0 kV, the maximum radial stress varying between 17·2 and 9·3 kV/cm. Since these bushings were made by four different firms and over a number of years, there are minor differences in the untapped bushing apart from the major difference between the simple and tapped-foil types. The minor differences are as follows:

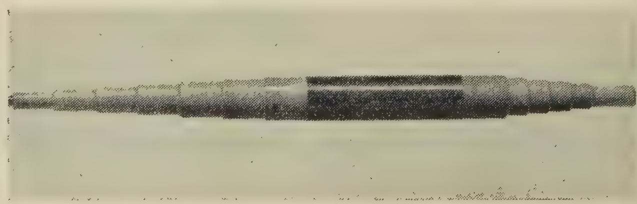


Fig. 1.—The type of bushing examined in the laboratory.

(*a*) The number of foils varies from 16 to 18.
(*b*) In most bushings the outer foil lies immediately below the banding wire, but in some, it is buried and is connected to the banding wire by means of a solder plug.
(*c*) Usually the foils are of metallized paper, the metal being tin. In other cases aluminium foil 0·001 in thick is used.

In the tapped bushings the outer or tapped foil is buried and is connected to a narrow section of banding wire by means of a solder plug as in (*b*). The major part of the banding wire is thus split axially into two sections, one on each side of this narrow section. They are connected together and to earth by the flange fittings.

(2.2) Characteristics obtained from Laboratory Power-Factor/Voltage Measurements

All bushings of the type and age mentioned and installed in substations near the laboratory were tested systematically. In this way, a random sample of 86 bushings from which the porcelain weathersheds and arc shields had been removed were examined.

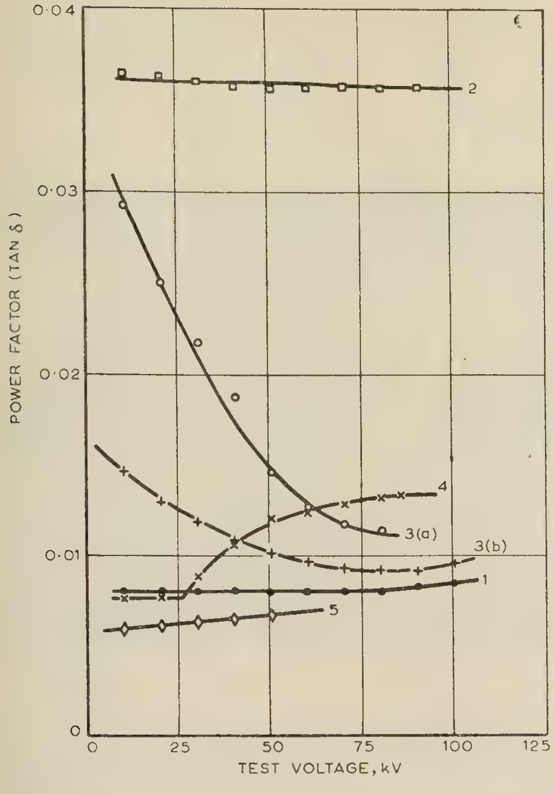


Fig. 2.—Typical power-factor/voltage curves.

As shown in Fig. 2 the power-factor/voltage characteristics obtained on the bushings fall into five distinct groups:

Group 1: Normal power-factor characteristic.
Group 2: High-level power-factor characteristic.
Group 3: Drooping power-factor characteristic.
Group 4: Low 'knee' point.
Group 5: Rising power-factor characteristic.

Table 1 shows the number of bushings in each power-factor-characteristic group.

Table 1

RELATIVE NUMBER OF UNTAPPED BUSHINGS FALLING INTO EACH POWER-FACTOR CHARACTERISTIC GROUP*

Maker	Year	Number of bushings in each Group					
		1	2	3(*a*)	3(*b*)	4	5
A	1931	24	0	3	4	1	4
B	1930	6	0	1	1	3	6
C	1929	12	3	2	0	3	1
D	1929	3	0	8	0	2	1

(2.2.1) **Normal Power-Factor Characteristic (Group 1).**

Of the bushings of manufacturer A, 24 out of 36 had flat characteristics typical of a new bushing. It can therefore be assumed that the properties of this group have not changed materially in service. In this way, the original power factor and capacitance of a bushing made by A in 1931 were found to be 0·006 5 and 202 pF, for B in 1930 they were 0·006 5 and 199 pF, and for C in 1929 they were 0·007 3 and 224 pF, respectively.

* Two bushings had two types of defect, so that in this Table the total number of bushings is considered to be 88.

Unfortunately most of the bushings tested which were made by D in 1929 altered during service, and it is not possible from the results to estimate the properties of a healthy specimen.

Only 52% of the bushings tested in this whole batch have retained their original properties and fall in Group 1, the remainder having changed in some way.

(2.2.2) **High-Level Power-Factor Characteristic (Group 2).**

The number of bushings with the distinctive high-level power-factor characteristic is small (only $3\frac{1}{2}$% of the number tested). Nevertheless the group is an important one.*

It is found that the high power factor is usually due to a low-insulation-resistance skin at the s.r.b.p. surface caused by water penetration. This was demonstrated on a typical bushing of this group by making connection to each foil and measuring the individual section properties. The equivalent power factor of this set of series capacitors was found to be 0·016, whereas the measured value for the bushing was 0·034. This difference was accounted for by the leakage in parallel with the capacitors—in this case, 5 000 megohms as measured at 5 kV d.c. Repeated tests over a period of time during which the bushing was stored in a warm room showed that the bushing dried out, its insulation resistance improving to 50 000 megohms and its power factor to 0·009. As this happened, the power factor, estimated by summing the properties of individual sections, became equal to the measured overall value.

A damp bushing if detected early enough shows no visible sign of deterioration, but if it is allowed to remain energized, tracking forms below the outer surface of the synthetic-resin bonded paper. Fig. 3 shows two views of the same part of the same bushing, one taken with normal photographic materials using intense illumination, the other with infra-red-sensitive plates. It is seen that the infra-red rays penetrate the synthetic-resin bonded paper sufficiently for the tracking to be visible. By skimming the surface of the bushing in a lathe it was found that the infra-red rays had penetrated to a depth of about 0·05 in, although tracking was found at much greater depths—certainly greater than 0·1 in.

The type of tracking described is different from that which occurs if the oil level in the upper porcelain weathershed is allowed to drop too low. Here, the tracking is on the surface and is caused largely by condensation of water drops on the exposed synthetic-resin bonded paper.

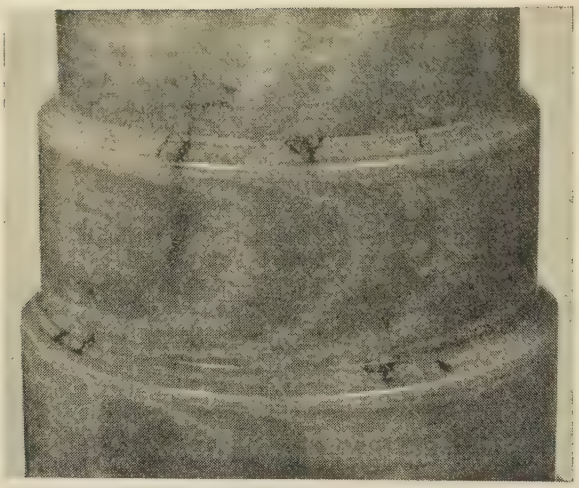
(2.2.3) **Drooping Power-Factor Characteristic (Group 3).**

Twenty-two per cent of the bushings of the whole sample fell into Group 3. This power-factor characteristic is caused by the presence in the bushing of a carbon path, the resistance of which depends on the current flowing through it. A carbon path has been found to exist in one of two places, either where an insulating section has punctured or where a poor contact exists at a buried earthed foil (or a tapped foil). In Table 1 the number of bushings of Group 3 which fall into Groups 3(*a*) and 3(*b*) are given.

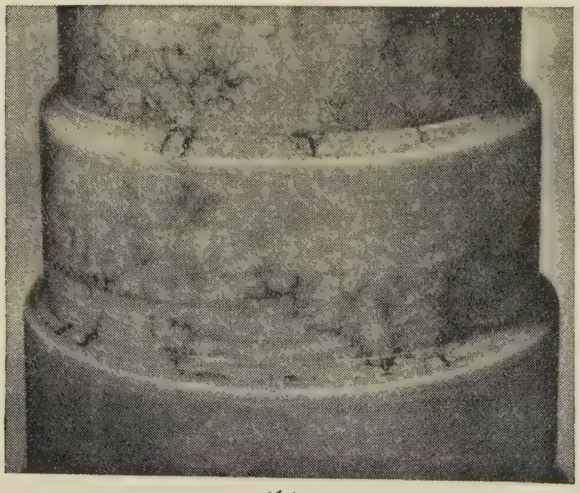
(2.2.3.1) *Punctured Sections* [*Group* 3(*a*)].

The effect of punctured sections on the power-factor characteristic is illustrated in Fig. 4. It will be noted that the drooping characteristic is always more pronounced if measurements are made with the voltage increasing. Often, if measurements are made on reducing the voltage, following a pressure test, the drooping characteristic virtually disappears. This is pre-

* In practice, the power factor often changes slightly with voltage but these departures from the straight line are found to be due to other minor defects in the bushings. When the minor defects are localized, e.g. confined to one insulating section, which can be short-circuited, the irregularities can be removed with little or no effect on the magnitude of the power factor. Thus, for present purposes, a straight line has been drawn for curve 2 of Fig. 2, as this is characteristic of the major defect.



(a)



(b)

Fig. 3.—Tracking below the surface of a bushing.

(a) Direct light.
(b) Infra-red light.

sumably due to a heating effect, because the power factor tends to change with time over the first few minutes after application of any voltage step.

In the case mentioned it was found that four sections, numbers 1, 3, 4 and 7 (counting from the conductor), were punctured. By short-circuiting these sections a flat power-factor curve was obtained.

Measurements of the resistance of each puncture path were made over the appropriate current range, and the power factor of the equivalent circuit consisting of four carbon puncture paths in series with 12 healthy sections was estimated. The result is shown in Fig. 4. The impedance of a bushing with n sections, of equal capacitance C and zero power factor, one of which is short-circuited by a puncture path of resistance R, is given by

$$\frac{R}{1+(\omega CR)^2} - j\left[\frac{n-1}{\omega C} + \frac{\omega CR^2}{1+(\omega CR)^2}\right]$$

If R depends on the current flowing, the capacitance of the equivalent circuit also varies with current, but the effect is small, the change being about one part in 10 000. As far as the overall

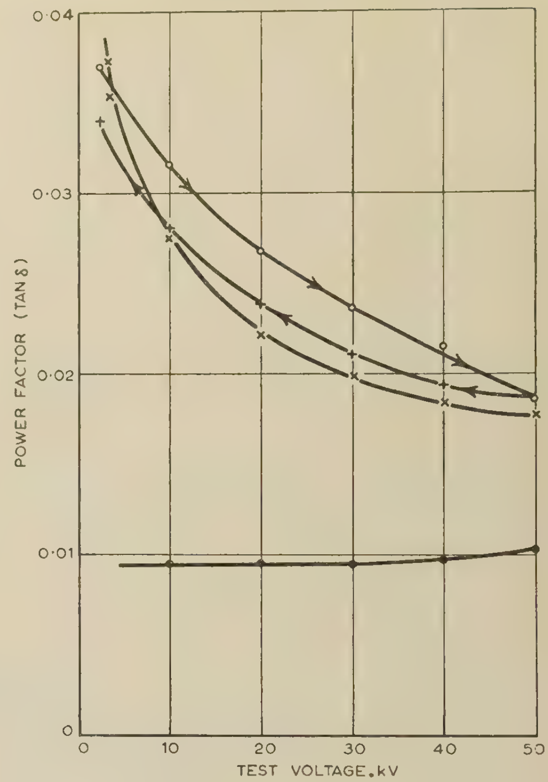


Fig. 4.—Bushing with punctured sections.

○ Whole bushing: increasing voltage (capacitance, 324 pF).
+ Whole bushing: decreasing voltage.
× Estimated for faulty bushing.
● Bushing with sections 1, 3, 4 and 7 short-circuited (capacitance, 325 pF).

bushing properties are concerned, the above expression approximates to

$$R - \frac{j(n-1)}{\omega C}$$

from which the power factor, tan δ, becomes $\omega CR/(n-1)$.

In the case of the above bushing, by short-circuiting three of

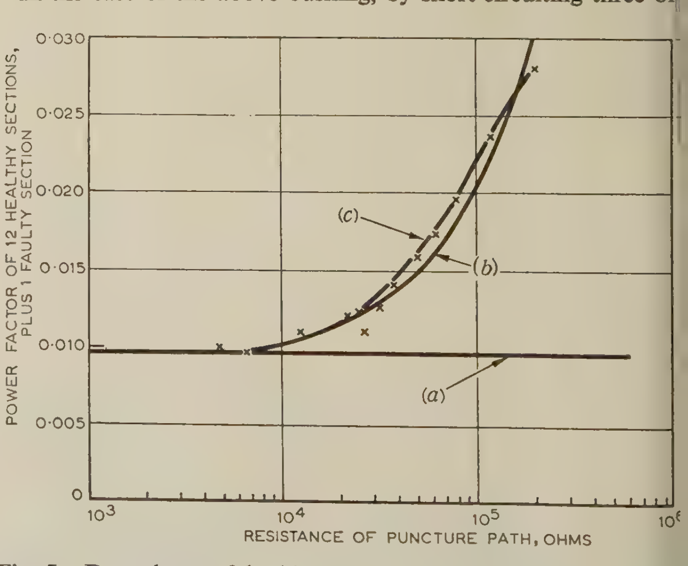


Fig. 5.—Dependence of bushing power factor on resistance of the breakdown path of one faulty section.

(a) Basic power factor.
(b) Calculated power factor.
(c Measured power factor.

the four faulty sections leaving the fourth in series with 12 healthy sections, the agreement between estimated and measured power factors could be checked over a considerable range of puncture-path resistance. Fig. 5 shows this agreement.

Insulating sections usually puncture at the foil ends as shown in Fig. 6. Generally the puncture follows a slow erosion process

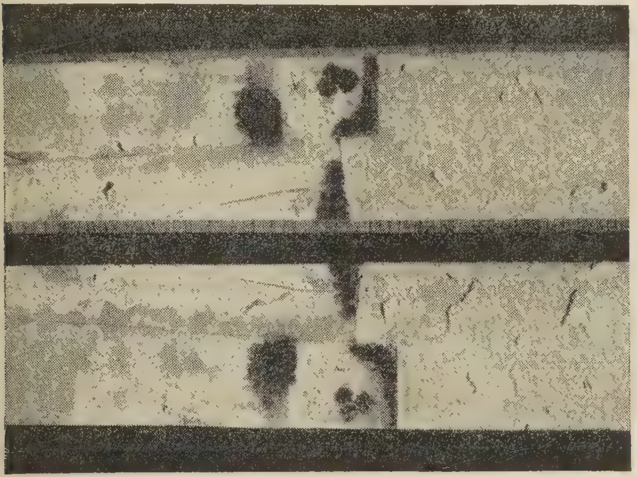


Fig. 6.—Erosion and puncture at badly overlapped foil end.

caused by discharge occurring in air spaces.[10] Most punctures occur at points of high stress caused by badly placed foils. Until the eroded hole actually penetrates the insulating layer it usually contains no carbon (Fig. 7).

Fig. 7.—Section through an eroded hole.

The mechanism of breakdown in simple solids has been described by Howard[11] and Mason.[12] It would appear that an initial uniform erosion is followed by the concentration of discharges at one or more preferred sites, with the result that deep eroded pits form. The deeper the pits become, the greater the quantity of discharge per pulse, and hence the greater the rate of increase of depth of the pit concerned. When the pit becomes deep enough for the stress at its tip to reach the intrinsic strength of the material, complete puncture occurs.

The problem has been discussed earlier, with particular reference to condenser bushings, by Kappeler.[13] It is shown[13, 14] that the voltage at which discharge commences at the edge of an unburied foil on the surface of an insulating sheet in air is given by

$$V_i = 8\cdot 1\left(\frac{d}{\epsilon}\right)^{0\cdot 45} \quad . \quad . \quad . \quad . \quad (1)$$

V_i = Inception voltage, kV (r.m.s.).
d = Thickness of the insulating layer, cm.
ϵ = Dielectric constant of the insulating material.

Kappeler considers that, in designing a bushing for use in power-supply systems, it should be assumed that there will be spaces at the ends of the foils, but that, even so, discharge should not occur at working voltage. Similar conclusions were reached by Silbermann[15] although his argument is based on the longitudinal field at the foil ends, this being determined graphically.

The ratio of working voltage to inception voltage derived from eqn. (1), for each insulating section of the types of bushing under discussion, varies from 2·6 to 1·2, and is greatest at the conductor and the banding wire and least in the middle of the bushing. Discharge and erosion would be expected to occur in these bushings at working voltage where air spaces at the foil ends exist. For the bushings under discussion, the radial thickness of each section was of the order of 3·5 mm, although there was considerable variation between bushings, radial thicknesses up to 5·5 mm being recorded. The thicker sections were found to contain eroded pits up to 4·5 mm deep in some cases. These depths are reasonably consistent with those predicted by Kappeler, the rate of erosion being about 0·2 mm per year. Reference to Mason's work[12] shows that such estimates of the rate of erosion should be treated with reserve because the rate increases as the pit deepens.

Current practice in bushing design is to use many more foils than formerly, a modern bushing replacement for those of the type discussed having about 60 foils instead of 16. Apart from other considerations, the use of thinner insulating sections increases the discharge inception voltage where foil-end voids exist. Thus, in such modern bushings, foil-edge erosion should not occur unless foils are inserted askew.

The commonest reason for the breakdown of a section is erosion, occurring mainly at the foil edge, but other types of breakdown have been found. In one bushing a carbon path was found between the innermost foil and the conductor. The path took the form of a long track passing through the butt joint of added insulation. In another case erosion had occurred at the periphery of an 'island' in the foil. Presumably cracks formed in the foil at some stage of manufacture isolating this small piece. The potential taken up by this piece would be different from that taken up by the main foil area, and would be determined by its relative spacing between the adjacent foils, whereas the potential of the main foil is also controlled by the foil lengths.

(2.2.3.2) *Faulty Foil Connection* [*Group* 3(*b*)].

Connections to the outer foil are made in only two cases:

(*a*) When the earthy foil is buried.
(*b*) When a test or metering tapping is brought out.

The trouble occurs where the thin foil is cut by the edges of the narrow brass or copper reinforcing tape. The connection between the foil and tape is then of high resistance and usually consists of one, or a series of, carbon paths in parallel, as shown in Fig. 8. The equivalent circuit of such a bushing is similar to that considered in the previous Section, except that none of the insulating sections is short-circuited. The capacitance of the bushing is therefore largely unaffected or reduced.

The case can be illustrated by considering the equivalent circuit of a tapped bushing, shown in Fig. 9. The characteristic measured between the conductor and banding wire is flat, whereas when tests are made to the tapped foil, whether connected to the banding wire or not, a drooping characteristic is obtained.

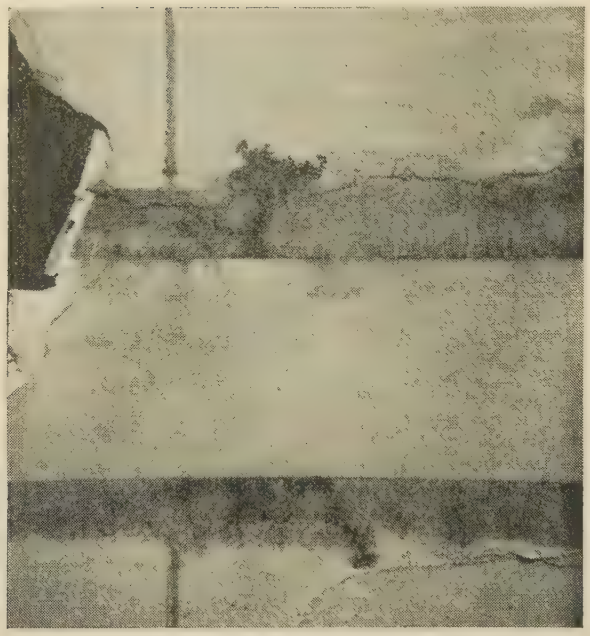


Fig. 8.—Disintegration of foil at the edge of brass tape tap-point reinforcement.

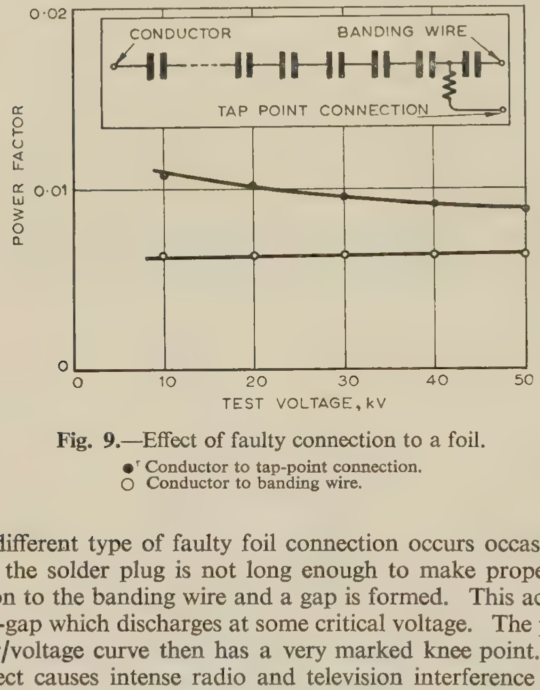


Fig. 9.—Effect of faulty connection to a foil.
● Conductor to tap-point connection.
○ Conductor to banding wire.

A different type of faulty foil connection occurs occasionally when the solder plug is not long enough to make proper connection to the banding wire and a gap is formed. This acts as a spark-gap which discharges at some critical voltage. The power-factor/voltage curve then has a very marked knee point. Such a defect causes intense radio and television interference and is usually detected in practice in that way.

In neither case is damage to the insulation severe, and although the conditions are most undesirable, it does not appear that there is any reason to suppose that the bushing is likely to fail as a result of these defects.

(2.2.4) **Low 'Knee' Point (Group 4).**

The power-factor/voltage test has long been used as a means of detecting discharge in internal voids in insulation and components such as bushings and cables. It is usually considered that any voids which may be present which discharge at the working voltage or less are most undesirable. Bushings with voids which discharge at very low voltages, 20 kV or so, have been detected. Invariably the increase of power factor is accompanied by a small increase in capacitance.

Dissection of bushings with very low knee points reveal that they contain spaces of large area—perhaps several square feet. These are usually located between the outer radius of a section and its bounding foil. The thickness of a space is of the order of 0·1–0·5 mm. Often the foil edge is blackened and non-conducting, but where the space does not embrace a foil edge, this blackening is not observed. The void is invariably filled with a light buff-coloured dust, and when splitting such a void a characteristic 'mousy' smell is noticed. The dust contains about 60% tin as determined spectrographically. Although the surfaces of both the tin foil and the synthetic-resin bonded paper had deteriorated slightly, no deep pits were found.

The voltage at which the power factor rises agrees well with the discharge inception voltage as measured with a discharge detector.[6] The actual inception voltages obtained are difficult to explain. A void of the dimensions mentioned above and remote from a foil edge would give rise to the following discharge inception voltage for any insulating section

$$V_i = V_p\left[1 + \frac{1}{\epsilon}\left(\frac{d}{a} - 1\right)\right]$$

V_p = Breakdown voltage of gas gap of length a.
ϵ = Dielectric constant of the insulating material.
d = Section thickness.
a = Void thickness.

A bushing discharge inception voltage of 20 kV implies a section discharge inception voltage of about 1 kV. A void of the dimensions mentioned could not have this effect unless either the dielectric constant of the insulating medium was locally very much greater than that of the bulk of the material or the breakdown strength of the void was much lower than that of air at s.t.p. The electrical properties of insulating material including dielectric constants are adversely affected by discharge in voids,[16] but it seems unlikely that this could account for the low inception voltage. It is probable that dust particles are responsible for this low breakdown strength, in which case the inception voltage probably started at about 50–60 kV when the space was filled solely with air and decreased through the years as the space began to contain hydrocarbon gases, carbon monoxide, etc., and dust.

Deterioration in these voids is undesirable, and where foil edges are involved, some stress control is lost when they are attacked. However, in those bushings examined, there was little evidence to suggest that failure of even one section was likely as a result of these spaces.

(2.2.5) **Rising Power-Factor Characteristic (Group 5).**

The difference between the characteristics of Groups 4 and 5 is often not very marked, especially if the knee point of a Group 4 bushing is very low. The power-factor curve, which rises steadily with voltage, is found to be associated with oil penetration into the bushing and usually only a few insulating sections are involved.

In one case, the power factor at both ends of the second insulating section was higher than in the centre and increased with voltage. This section was visibly saturated with oil which had penetrated from both ends but had not reached the centre. A somewhat similar case was reported by Brownlee and Wickham.[17]

Among others, Sticher and Piper[18] have shown that oil in a discharging air space deteriorates rapidly, products with high

power factor being formed. It is probable that this is the reason for the high power factor in the regions affected by oil ingress. At room temperatures the power factor of the oily material increases with voltage, but at higher temperatures the curve is humped. Curves of this shape occurring with impregnated paper insulation have been attributed to the migration of ions being restricted by cellulose barriers.[19]

As far as can be seen this defect does not impair the efficiency of the bushing electrically, as the local high losses are not high enough to cause thermal instability. Mechanically, however, such a bushing would be weaker, and there may be a risk of the bushing telescoping when subjected to axial forces. The bushing would also be less effective as an oil barrier.

(2.3) Limitations and Practical Importance of the Observations

One bushing which failed in service was found to contain sequences of self-healed punctures. This condition has been observed in other bushings that have been dissected. It would appear that, when an eroded path penetrates the section, there is a risk that sufficient energy is liberated to cause the foil to volatilize at the point of impact. If the foil is thin enough and of a low latent-heat material, no carbon path bridges the section concerned. If this happens a serious condition arises because the eroded hole can continue through the next section at an ever-increasing rate, possibly culminating in rapid complete breakdown of the bushing. This defect is not revealed by the non-destructive tests at present normally in use, although it is possible that it would be shown by the discharge detector. It would appear from observation and calculation that the process can take place if the foils are of tin and of the order of 10^{-4} in thick. It could not occur with a thicker foil, and the 10^{-3} in-thick aluminium foils sometimes used would appear to be completely safe and would always terminate an eroded puncture path.

In another type of defect foils have been found to be disintegrated. This condition can be detected by radiographic examination (Fig. 10), but it does not appear to affect the power

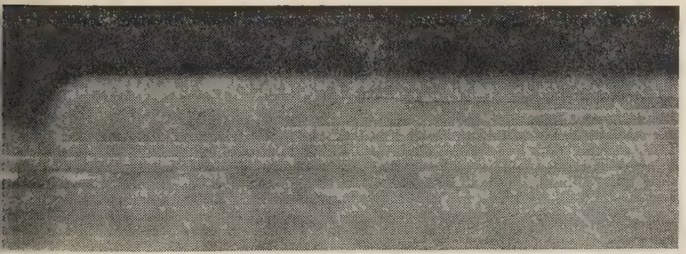


Fig. 10.—Radiograph showing disintegrated foils.

factor and capacitance of the bushing. The metal appeared to have melted and contracted to form lumps, and yellow powder such as is found in voids was present. The reason for the occurrence of this condition is not known—possibly it is associated with self-healed punctures.

The destructive examination of bushings with typical characteristics has provided data which help to interpret the characteristics of bushings and to predict their condition by non-destructive tests. It has been shown that water penetration can lead to direct failure in service. Also a bushing containing punctured sections is over-stressed and probably contains other sections which are deteriorating. It is considered that bushings with these defects are unsuitable for further service. The other defects discussed—poor tap-point connections and the presence of voids or oil—are not likely to lead to rapid failure, and provided that regular testing is carried out to detect the next stage of deterioration (the puncture of a section), there is no reason why these bushings should not give years of further trouble-free service.

Of the 86 bushings examined, 52% were unchanged by their years of service, 20% were definitely faulty and the remaining 28% contained defects but were fit for further service.

The erosion process at the foil ends is extremely slow. Therefore bushings with only one or two sections punctured out of 16 or 18 are not in imminent danger of failure. Bushings have been detected with six out of 18 sections punctured before being removed safely from service. Also one bushing with probably five sections punctured and another with a large void have been on extended life test at 110% of working voltage for 16000 hours. No further deterioration has occurred, the changes taking place with time being completely accounted for by temperature changes. In fact, they have enabled the temperature coefficient of capacitance to be estimated, and this is found to be determined by the coefficient of linear expansion of the synthetic-resin bonded paper.

Other tests have proved useful as laboratory tools. Radiography enables misplaced foils, cracks and build-up insulation to be detected and located (see Figs. 10 and 11). The discharge

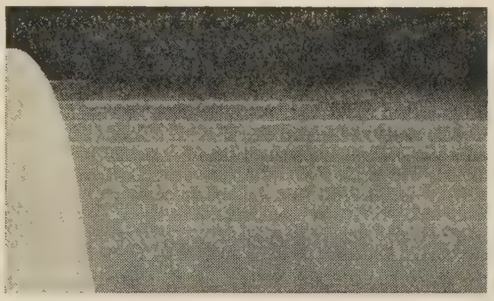


Fig. 11.—Voids at foil ends.

detector has confirmed that the power-factor/voltage curve shows the presence of discharging voids for all practical purposes. Infra-red photography has proved useful, but can only be used when tracking below the surface is suspected, as it is impracticable to apply it as a routine test.

(3) SITE TESTS

On site, conditions are much less favourable than in the laboratory, but it is impracticable to transport all bushings to the laboratory and it is necessary to extend the principles developed to the field. In most cases, measurements are made with the l.v. side of the test object permanently connected to earth, the bushing being completely assembled and mounted in the breaker tank. In addition, the tests must usually be carried out in the presence of extraneous electric and magnetic fields.

Since the shape of the power-factor/voltage characteristic is an important criterion in judging the condition of a condenser bushing, it was felt that a portable Schering bridge, operating over a voltage range to give the significant portion of the power-factor/voltage curve of a 132 kV bushing, would give the most useful information from site tests. Accordingly, a portable Schering bridge was constructed, 30 kV being selected as the maximum test voltage at which the instrument would remain reasonably portable. It was decided that the bridge should be of the conventional type, adequately screened for earthed tests, in preference to an inverted Schering bridge which has yielded useful results in America.[20]

(3.1) Description of 30 kV Portable Schering Bridge

The conventional Schering bridge is too well known to be described in detail, but a few features of the instrument are discussed. The present bridge has been designed primarily for measurements on earthed samples in the field, but it is adaptable for normal operation where the test sample is insulated from earth. The bridge is double-screened throughout, though if it is to be used solely for site measurements, the low-voltage bridge arms need to be surrounded by a single earthed screen only. For ease of transportation, the bridge has been built in three separate parts:

(i) The power unit and standard 100 pF mica condenser.
(ii) The bridge unit.
(iii) The detector.

To enable the bridge to be balanced as rapidly and accurately as possible, an amplifier-detector unit has been developed in which the sensitivity control is entirely automatic. This is accomplished by using a combination of limiter circuits and negative-feedback amplitude control, together with a band-pass tuning system to eliminate the effects of harmonics in the supply voltage. In addition to the amplifier-detector section, the instrument contains an amplifier by which the inner screens of the bridge are driven at a voltage equal to the mean potential of the two bridge detector points. The function is similar to that performed by a conventional Wagner earth, with the considerable advantage that the process is automatically performed throughout the bridge-balancing procedure.

(3.1.1) Sources of Error in the Bridge Measurements.

(3.1.1.1) *Stray Capacitance in the Bridge Circuit.*

The errors due to stray capacitance from the low-voltage bridge arms to the inner screens are negligible during site measurements, since great accuracy is not required. For high-precision laboratory measurements, the screen-drive circuit reduces these errors. During measurements on earthed bushings, the inner screen of the high-voltage cable is connected directly to the bottom point of the bridge; this results in the capacitance between inner and outer screens being connected across arm 3 of the bridge (Fig. 12) and introduces an error into the power-factor measurement. Tests in the field, however, have shown that there is no necessity for the outer earthed screen on the high-voltage cable, and satisfactory results have been obtained with one screen connected to the bottom point of the bridge. The stray capacitance from this screen to earth is then too low to introduce any significant error.

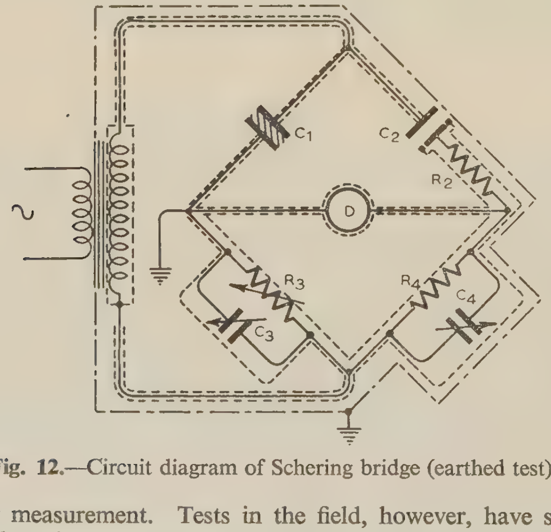


Fig. 12.—Circuit diagram of Schering bridge (earthed test).

(3.1.1.2) *Stray Capacitance from the High-Voltage Side to Earth.*

Although the test transformer is screened, there is inevitably some stray capacitance from the high-voltage side to earth, which constitutes a second condenser in parallel with the test sample. If C_0 and $\tan \delta_0$ are the capacitance and power factor of these strays, and C_1 and $\tan \delta_1$ are the values obtained from tests on an earthed sample, it can be shown[21] that the true capacitance and power factor of the sample are given approximately by

$$C = C_1 - C_0, \text{ and } \tan \delta = \frac{C_1 \tan \delta_1 - C_0 \tan \delta_0}{C_1 - C_0}$$

Table 2 shows the close agreement between the results of corrected measurements on an earthed sample and on the sample insulated from earth.

Table 2

RESULTS OF SCHERING-BRIDGE MEASUREMENTS ON A BUSHING EARTHED AND INSULATED FROM EARTH

Test voltage	Bushing earthed		Bushing insulated from earth	
	Tan δ	Capacitance	Tan δ	Capacitance
kV		pF		pF
5	0·0072	237	0·0079	221
10	0·0071		0·0078	
20	0·0073		0·0079	
30	0·0099		0·0104	

The stray capacitance from the bushing to earth is included in the first measurement but excluded from the second, and accounts for the higher capacitance during the first test. The power factor of this stray capacitance is small and the effect is to 'dilute' the measured power factor of the bushing during the earthed test.

(3.1.1.3) *Capacitive Coupling with High-Voltage Conductors.*

It has been stated previously[1] that the effects of induced voltages in the test bushing, due to capacitive coupling with neighbouring high-voltage conductors, can be eliminated by reversing the supply to the bridge network and taking the mean of the two sets of bridge readings. Table 3 illustrates the effects of electric and magnetic fields on the bridge readings. This method of eliminating 'pick-up' on site has proved to be satisfactory, except in a few cases where the interference was very severe and the difference between the two power-factor readings at low test voltages was so great that the lower power factor appeared to be negative. Under these conditions it was impossible to balance the bridge, and it was necessary to use the capacitor C_3 to increase both bridge readings. In extreme cases the existing ten × 0·001 μF decade capacitor is inadequate and a larger capacitance is needed.

Recently, details have been given[22] of a phase-shifting device included in the primary of the test transformer, which, it is claimed, successfully eliminates the effects of electric and magnetic fields. This appears to be an unnecessary refinement, since the simple reversal of supply accomplishes the desired result without any additional equipment.

(3.2) Site Test Procedure

(3.2.1) Insulation-Resistance Measurements.

The insulation resistance of each oil-circuit-breaker bushing was measured at 5 kV d.c. using an electronic test set, and the insulation resistances of the lift rods were checked by testing

Table 3

EFFECTS OF ELECTRIC AND MAGNETIC FIELDS ON SCHERING-BRIDGE READINGS

Test voltage	R_4	R_3	C_1	C_4	tan δ	Test conditions
kV	ohms	ohms	pF	μF		
5+	10 000/π	1 296·8	256	0·033 2	0·033 3	No interference.
−	10 000/π	1 303·5		0·033 4		
30+	10 000/π	1 299·2	256	0·033 3	0·033 3	
−	10 000/π	1 300·9		0·033 4		
5+	10 000/π	1 307·1	256	0·019 4	0·033 2	20 kV on a conductor 4 ft from bushing.
−	10 000/π	1 293·1		0·047 0		
30+	10 000/π	1 300·7	256	0·032 2	0·033 3	
−	10 000/π	1 299·0		0·034 4		
5+	10 000/π	1 298·3	256	0·033 5	0·033 4	Conductors carrying 300 amp 3 ft from bushing.
−	10 000/π	1 297·8		0·033 2		
30+	10 000/π	1 298·0	256	0·033 4	0·033 4	
−	10 000/π	1 297·9		0·033 4		

each phase to earth with the breaker closed. All insulation resistances are corrected[23] to 20° C.

The results of field measurements of insulation resistance have confirmed that a relative humidity of 70% is the limit for testing without guard wires if the porcelain is clean, but very low surface leakage resistances have been recorded where the porcelain weathersheds were polluted and when the humidity was less than 60%. Therefore, it is the practice to use guards whenever low insulation resistances are encountered, irrespective of the humidity.

It is important to note that if surface leakage is excessive, faulty readings of insulation resistance may be obtained even when guards are used, the error depending on the internal resistance of the test set. Therefore, if after cleaning the porcelain weathershed the surface leakage resistance is less than 1 000 megohms (for the particular test set being used), it is recommended that testing should be postponed until the surface has dried.

(3.2.2) **Schering-Bridge Measurements.**

After disconnecting the bushings from the busbars and cleaning the porcelain weathersheds, bridge measurements were made at 5, 10, 20 and 30 kV. To observe any power factors which change with voltage, it is essential to measure the power factor at increasing voltage steps, as discussed in Section 2.2.3.1. Bridge measurements were not made with the breaker closed, since lift rods are best checked by means of insulation-resistance tests.

The stray losses from the high voltage to earth were measured by balancing the bridge with the high-voltage cable disconnected from the bushings. It has been found that these stray losses vary appreciably with temperature, and it is necessary to check frequently the capacitance and power factor of the stray losses during bridge measurements.

In most 132 kV oil circuit-breakers the two bushings of the centre phase have tap connections to the first buried foil, and these tappings are an alternative means of checking the bushing insulation. Unfortunately, they are often of high resistance, resulting in drooping power-factor/voltage curves.

The use of guard rings during Schering-bridge tests to eliminate surface leakage effects on bushings is unsatisfactory. In practice, testing has been discontinued whenever the surface leakage resistance has fallen below 5 000 megohms—usually when the humidity exceeds 75%.

No temperature corrections are made to power factors or capacitances since they do not vary significantly over the temperature ranges encountered in practice.

(3.3) **Results of Site Measurements**

Over 1 600 132 kV oil-circuit-breaker condenser bushings have been tested *in situ* by means of the 30 kV portable Schering bridge, the majority of these being of the condenser type with oil filling. The insulation resistances of most of the bushings were also measured.

Sixty-three of the bushings have been or are being removed from service; of these, 61 had high power factors which drooped with voltage and high capacitances, whilst the remaining two bushings were damp and were detected by their high-level

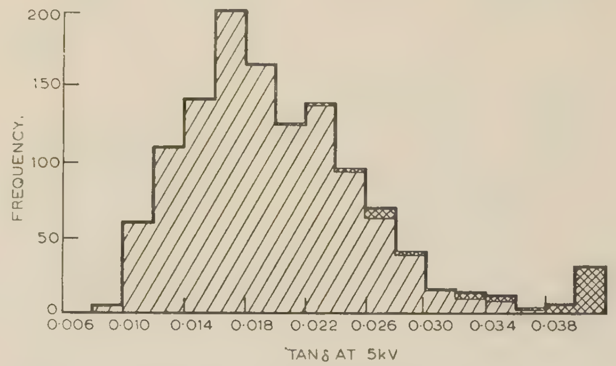


Fig. 13.—Power factor distribution of bushings tested *in situ*.
Serviceable bushings.
Rejected bushings.

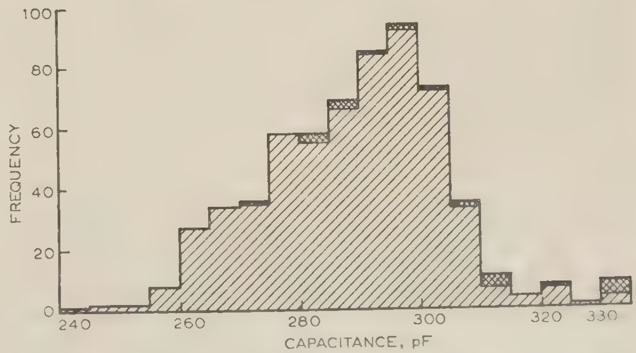


Fig. 14.—Capacitance distribution of bushings tested *in situ*.
Serviceable bushings.
Rejected bushings.

power factors and low insulation resistances. Another 43 are classified as being in doubtful condition and will be retested every two or three years to obtain information about the rate of insulation deterioration. The limits for classification of these deteriorated bushings are discussed in the following Sections.

The number of bushings tested is too large to be given in detail, but Figs. 13 and 14 show typical power-factor and capacitance distributions of a number of oil-circuit-breaker condenser bushings.

To check the results of site measurements, all faulty bushings are returned to the laboratory, and laboratory Schering-bridge measurements have confirmed the diagnoses made *in situ*.

(3.4) Discussion of Results of Site Tests

(3.4.1) Insulation-Resistance Measurements.

Since moisture present in condenser bushings is more or less uniformly distributed over a thin surface layer of the dielectric, it can be detected by insulation-resistance measurements. During site tests on over 1 600 bushings, only two damp condensers were found. But bushings fitted with s.r.b.p. shrouds at their oil ends in place of the normal porcelain arc shields are particularly susceptible to ingress of moisture from the oil in the breaker tank, and many bushings fitted with these shrouds have low insulation resistances. Insulation resistance is more sensitive than power factor for detecting moisture in insulation as illustrated by a damp bushing where moisture increased the power factor by less than a factor of two, whereas insulation resistance was lowered by a factor of 30.

The presence of the breaker oil, lift rods, explosion pots, arc shields, etc., all of which affect power factor and are discussed in Section 3.4.2, has no effect on insulation resistance provided that the insulation of these components is in good condition. Thus, for the correct interpretation of site measurements, a knowledge of insulation resistance is essential, in addition to power factor and capacitance.

To obtain the maximum information from insulation-resistance measurements, the test results, corrected to 20° C, should be recorded annually and the trend of the insulation resistance of each insulating component noted. For example, if six bushings in a circuit-breaker are tested and one is found to have a distinctly lower insulation resistance compared with the other five, it would be suspect, irrespective of the actual insulation resistance disclosed by the test. Further investigation would naturally be made in order to determine the exact cause of the low insulation resistance of the one bushing.

In the absence of comparative data, the following limits may be applied to 132 kV oil-circuit-breaker condenser bushings. Under ideal conditions, the insulation resistance of individual insulating components should exceed 20 000 megohms, but under normal site conditions, test figures of 5 000–10 000 megohms are obtained on insulators in good order with respect to moisture. A figure of less than 1 000 megohms at 20° C indicates a faulty bushing and further investigation is required. Resistances between 1 000 and 5 000 megohms indicate that the insulation is in doubtful condition.

(3.4.2) Power-Factor Measurements.

Fig. 13 shows the distribution of installed bushing power factors, the average value being 0·020 compared with 0·007 in the laboratory. Investigations have shown that the presence of breaker oil, explosion pots or de-ion grids, lift rods and arc shields all contribute to the higher power factors of installed bushings. For instance, lowering the level of the oil in the circuit-breaker tank may reduce the bushing power factor by 30%. The change in power factor may be partly accounted for by the difference in permittivity between oil and air, but other factors contribute to the higher values of tan δ. In many cases removal of explosion pots reduced the power factors appreciably; this may be explained by considering the explosion pot as a low capacitance of high power factor in series with stray capacitance of low power factor to the tank.

When tests are made on the condenser parts only in the circuit-breaker tank, or when using a tapping connection, the power factors are comparable with results of laboratory measurements. Thus it is possible for bushings to be in sound condition in spite of site power factors being abnormally high.

For these reasons it is not possible to define a power-factor rejection limit. If a limit were fixed at, say, 0·03, several faulty bushings would be left in service whilst an appreciable number of sound ones would be removed unnecessarily. Change of power factor with voltage is an indication of punctured sections, and, therefore, if power factor varies by more than 0·005 between 5 and 30 kV the bushing is removed from service. If power factor varies with voltage but is less than 0·005 between 5 and 30 kV, the bushing is classified as being in doubtful condition and should be rechecked every two or three years.

The power factor or change of power factor with voltage is not related to the amount of insulation deterioration.

(3.4.3) Capacitance Measurements.

Site capacitances of oil-circuit-breaker bushings are approximately 40% higher than the values obtained from laboratory measurements, although this figure naturally varies with the type of switchgear, bushings and manufacturers. Stray capacitance from bushing to breaker tank, which is increased by the presence of the breaker oil, is the main cause of the increase in capacitance, but the measured values are also affected to a lesser extent by operating rods, explosion pots or de-ion grids and porcelains.

Fig. 14 illustrates the large range of capacitance of a number of bushings installed in the same type of switchgear. The coefficient of variation of capacitance is approximately 5%. There is evidence that the spread of capacitance of bushings manufactured at later dates is appreciably less, and it may be possible in future to detect faulty bushings by statistical analysis of the test results.

In the meantime, it is not possible to fix definite rejection limits and it is necessary to look for increase in capacitance with age. Any increase of 10% or more is regarded as an indication that a bushing should be removed from service. Also, if the capacitance of a bushing in an oil circuit-breaker is greater than 10% above the average for that particular switch, it is regarded as being in doubtful condition.

(3.5) Recommended Test Procedure

The object of site measurements on condenser bushings is to determine whether the insulation is safe to be left in service until the next routine check. Failure can occur by erosion through the insulation or by tracking over the surface of the s.r.b.p. condenser. Breakdown between foils gives rise to an increase in capacitance, unless the bushings are of the type with very thin foils, discussed in Section 2.3. Partial tracking is extremely difficult to detect on site except by visual examination, but it can be prevented by keeping the s.r.b.p. insulation completely covered with clean dry oil; low oil levels can be seen by inspection, and moisture in the oil detected by low insulation resistance. Thus the majority of bushings, the insulation of which has deteriorated appreciably by ingress of moisture or erosion, can be detected by regular measurement of insulation resistance and capacitance.

Site measurement of capacitance is a comparative test, but as the majority of old 132 kV condenser bushings in this country have been checked by Schering-bridge tests and their capacitances

are known, future measurement of capacitance will reveal any further deterioration. The capacitances of new condenser bushings supplied by the manufacturers are of little value for comparison with the results of site measurements, and it will be necessary to measure the capacitance of each new bushing after installation in its breaker. Although regular capacitance measurements should be unnecessary for the first few years of the life of a bushing, insulation resistance should be recorded annually.

A combined insulation resistance-capacitance test set using an electronic null detector has been constructed. The capacitance bridge operates at 5 kV, and limited experience in the field has shown close agreement with capacitance as measured by the Schering bridge. No provision has been made in the bridge for the measurement of power factor, as it operates at one voltage only, and previous results have shown that power-factor measurement at a single voltage is not a reliable indication of insulation deterioration.

(4) CONCLUSIONS

A comprehensive programme of test of a large number of old condenser bushings has shown that the insulation may deteriorate in service in the following ways:

(*a*) Ingress of water.
(*b*) Erosion at foil edges resulting in puncture of sections.
(*c*) Deterioration of connections to buried foils.
(*d*) Formation of voids or cracks.
(*e*) Penetration of oil.

These conditions can be diagnosed in the laboratory by power-factor/voltage tests.

Water penetration or erosion is considered to be sufficient to warrant removing a bushing from service; the other three defects will not lead directly to breakdown.

Most old 132 kV bulk oil-breaker condenser bushings in the country have been tested *in situ* by means of a portable 30 kV Schering bridge. With site measurements, the shape of the power-factor/voltage curve is much more important than the absolute values of power factor, and as the greatest change of power factor with voltage occurs at the lower test voltages, the maximum test voltage of 30 kV is adequate. If tests are carried out at one voltage only, a low test voltage is advantageous, power-factor tests at working voltage being of little value: 4% of the bushings tested on site were found to be faulty and 3% in doubtful condition.

It is concluded that annual insulation-resistance measurements and inspection of bushing oil levels will detect the conditions which lead to surface tracking, whilst annual site measurements of capacitance will detect punctured sections of condenser bushings.

(5) ACKNOWLEDGMENTS

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[The discussion on the above paper will be found overleaf.]

DISCUSSION BEFORE THE SUPPLY SECTION, 29TH JANUARY, 1958

Mr. F. C. Walmsley: From the assessment provided, two principal factors emerge, namely moisture and erosion due to discharges. Hence from Table 1, Groups 2 and 5 cover (i) and Groups 3 and 4 cover (ii).

Group 2 bushings have been affected by the ingress of moisture due to constructional methods which obviously could and have been improved. I feel that the higher power factors of Group 5 bushings have been caused by the replacement of air with oil having a higher loss and possibly migrated moisture from the circuit-breaker oil.

The effects of internal discharges were not appreciated by bushing designers 30 years ago, since the use of s.r.b.p. was relatively new for 132 kV bushings, and the approach then was largely coloured by thermal stability and electric strength requirements. Moreover, the Schering bridge was a new dielectric-loss-measuring technique. Subsequently the 'hissing' test closely followed by power-factor/voltage measurements became the recognized means of ascertaining discharges in bushings. Now discharge bridge measurements represent modern detection methods, which, with *proper interpretation* are acceptable for safeguarding quality. As shown in Fig. A, the

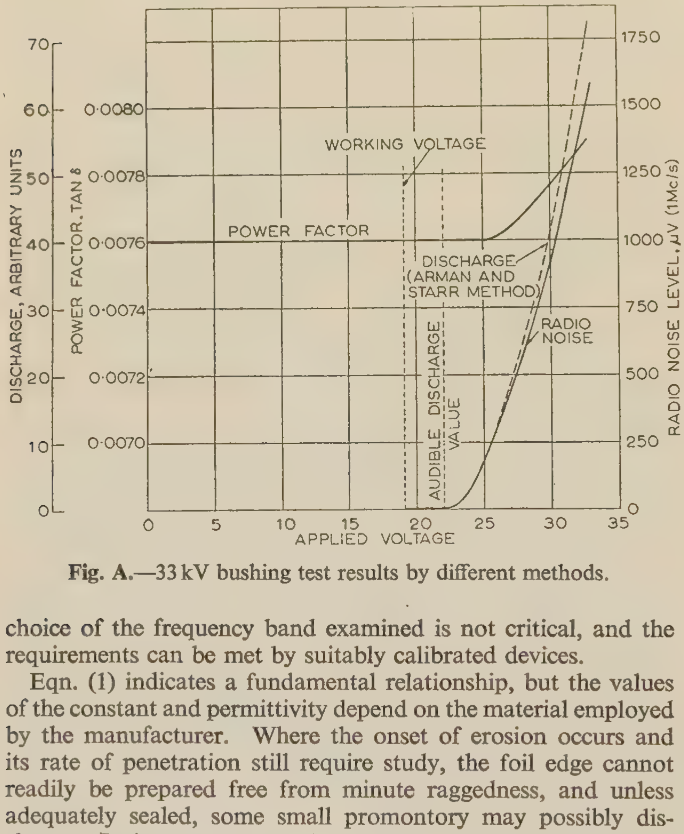

Fig. A.—33 kV bushing test results by different methods.

choice of the frequency band examined is not critical, and the requirements can be met by suitably calibrated devices.

Eqn. (1) indicates a fundamental relationship, but the values of the constant and permittivity depend on the material employed by the manufacturer. Where the onset of erosion occurs and its rate of penetration still require study, the foil edge cannot readily be prepared free from minute raggedness, and unless adequately sealed, some small promontory may possibly discharge. It does not necessarily follow that misplaced foils will lead to breakdown at the corner, and evidence found supports this view.

I have in progress accelerated life tests at 10 kc/s to examine the erosion factor, and in cases where, with some samples, the erosion slowed up and finally ceased, without breakdown, an explanation is being sought.

The improvement in performance, provided by additional foils, is well known to manufacturers, and techniques have been devised to provide, in effect, a greater subdivision than that mentioned. Additional foils are not always a complete solution, and since they possess a finite thickness they can cause loss of bonding pressure in the rolling process with the possibility of void formation.

In Section 2.2.4 the authors give some information and their views on bushings having a low 'knee' point. Granted that a foil or foils are showing poor adhesion, the 'knee' value mentioned is extremely low, but it is difficult to accept their explanation, in the more likely event of the void possessing much smaller radial dimensions than those quoted. Modern bushings have considerably improved foil adhesion, but practice, as yet, has not eliminated the possibility of the presence of discontinuous fine circumferential cracks in the insulation.

The authors have provided helpful information in Section 3, and their views on criteria for judging the condition of bushings in the field are most welcome. They have justified the employment of the power-factor/voltage technique, but although now there is less tendency to spread in capacitance, it should be recognized that each manufacturer's product will have its characteristic spread and generalization would be unwise.

The fact that so few bushings have been troublesome augurs well for the future, since the product for some years past has been subjected to more exhaustive acceptance tests, and the supply undertakings should be assured of reliable behaviour for the expected life of the associated equipment.

Dr. J. S. Forrest: The work described in the paper is part of a programme of research which we have been carrying on for many years at Central Electricity Research Laboratories, and the paper may be looked upon as a sequel to one read 16 years ago. The main conclusions established in that earlier paper still stand, but the authors have made two notable advances.

In the first place, they have elucidated completely the cause of the drooping power-factor characteristic. When we first met this phenomenon, insulation experts with whom we discussed the matter said that they had never encountered it, and, moreover, that they did not believe it. It appears very simple as presented in the paper, but, in fact, it required a great deal of ingenuity to discover the mechanism of this type of failure. Incidentally, I feel that the title of the paper hardly does justice to this aspect of the work, which involved quite a brilliant piece of research and not merely laboratory and field tests on bushings.

Secondly, the authors and their colleagues in the Divisions have tested 1 600 132 kV bushings and have found some 60 faulty ones. This is a major contribution to continuity of supply. I do not know the average cost of a 132 kV fault, including the incidental costs resulting from circuit outage, which may be very high. But even if we take the modest figure of £1 000 per fault, it is clear that the authors have paid for the cost of the research many times over.

Finally, I should like to comment on the amount of field testing one should do. As in many engineering problems, a practical working compromise must be found. At one extreme, there is no need to do any tests; faulty insulators will then blow up and locate themselves very effectively. If too many insulators of one type blow up, the whole lot have to be replaced by more from another manufacturer. At the other extreme, we could continuously monitor the condition of every insulator in the system and give an alarm if the power factor or insulation resistance deviated from normal. Technically, this is quite possible, but apart from very special installations, the cost, and above all the maintenance of the test equipment, would greatly outweigh the advantages. Between these two extremes there is a whole range of possibilities, from an occasional insulation-resistance test at 500 volts to a routine power-factor/voltage test at 100 kV or more. As a result of our experience for more than

20 years, we have reached the conclusion that, taking into account costs, convenience and the availability of technical staff, the best practical working compromise is to make two simple tests—an insulation-resistance test at 5 kV d.c. (there is no need to go any higher) and a capacitance measurement at a few kilovolts a.c.

If these two simple tests are carried out annually, it is possible to detect more than 90% of insulator faults before complete breakdown occurs in service.

Mr. P. G. Ashley: The results which the authors have obtained in the laboratory on a group of about 90 bushings are similar to those I have encountered on about 200 bushings returned to the works for reconstruction. These are 132 kV condenser bushings with plastic compound filling and have been in service for up to 27 years. A few of these have an unsatisfactory characteristic as shown in Fig. B, where curves D1 and E1 refer to

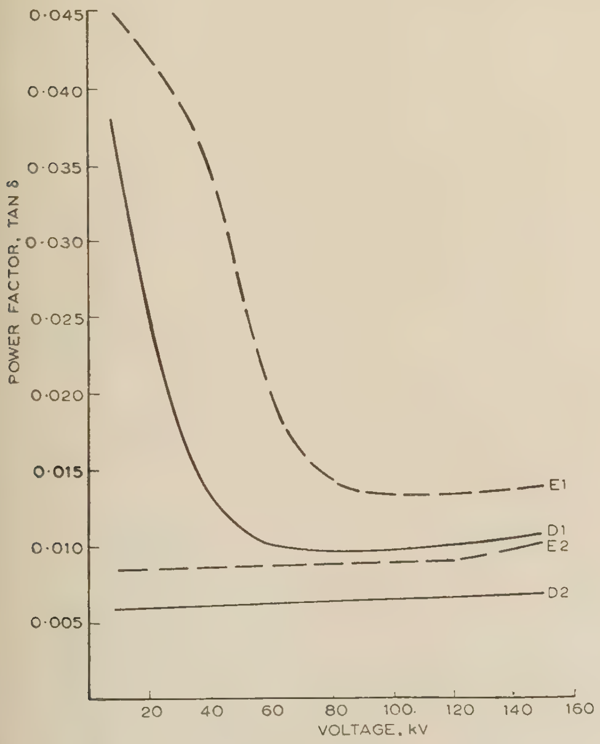

Fig. B.—Power factor of bushing assembly (D1 and E1) and bushing core only (D2 and E2) for bushings 25 years old.

bushings as received from site, and D2 and E2 are for the bushing cores immersed in oil at normal temperature after dismantling. The dismantling does not have much effect on the bushing core, since the temperature for removing the compound is only 70° C for 24 hours. The curves show that the bushing cores are satisfactory, although the assemblies are not.

Where high power factors have been found, attempts have been made to dry out the insulation by oven treatment at 100° C for 72 hours after varnish removal. Fig. C shows that, whereas a typical bushing has improved from curve A1 to curve A2, two others have deteriorated from B1 and C1 to B2 and C2. In the first case I believe that surface moisture has been removed; in the others the moisture may have been more deep-seated, or perhaps there has been a chemical change.

Fig. D summarizes the power factors at 100 kV for the 200 or so bushings examined, plotted against age; and there is a general trend of increase of power factor with age. We have not been able to account for the difference in behaviour of the location 1

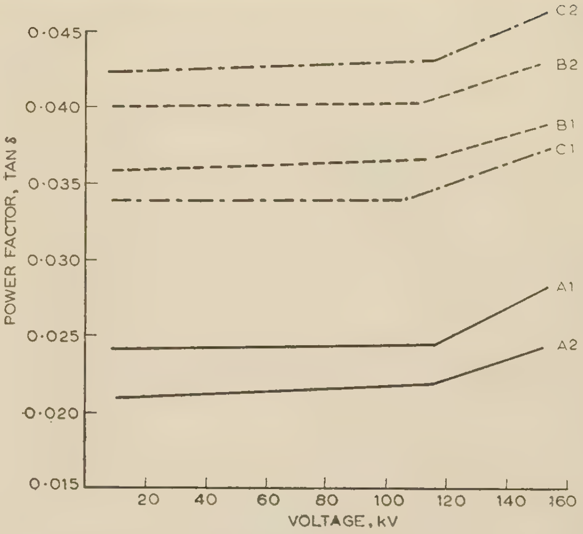

Fig. C.—Some effects of drying out the bushing cores at 100° C for 72 hours after varnish removal.

A1, B1 and C1: Before drying.
A2, B2 and C2: After drying.

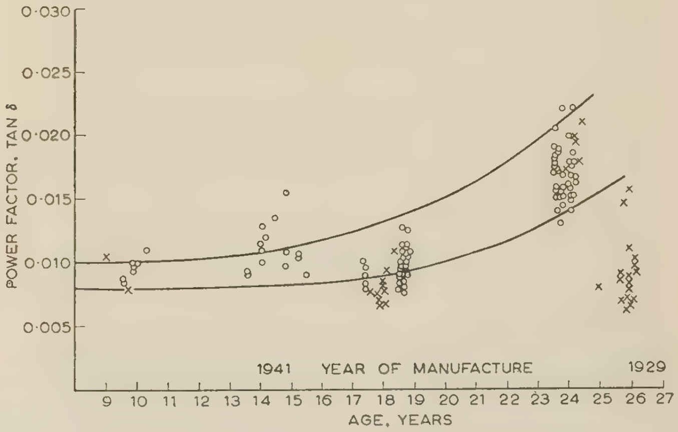

Fig. D.—Power-factor/age characteristics at 100 kV for 200 bushings.

× Bushings installed in location 1.
● Bushings installed in location 2.

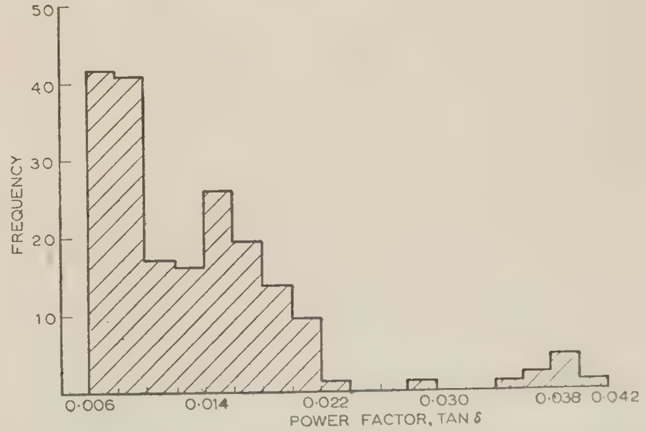

Fig. E.—Power-factor distribution at 100 kV of 200 bushing cores tested at the manufacturers.

Those with values above 0·030 were rejected.

bushings compared with those from location 2. Attention is drawn to bushings made in 1941, when manufacturing conditions and personnel were adversely affected by war conditions. I think it is likely that these bushings had higher power factors than those made before and after 1941, although records are not now available to establish this.

Fig. E shows my findings as compared with those in Fig. 13. Of the 200 bushings examined 27 have been rejected; seven for intrinsic high power factor (above 0·03); six for unsatisfactory power-factor/voltage curve [these fell in the authors' category (3)], and 14 for internal discharge characteristics which were above the general level.

Mr. W. J. Brown: It is important to note that a very considerable proportion of the old bushings which the authors tested have suffered no deterioration whatever after 25 years' service; and those which have deteriorated are found to contain defects which, with the exception of the absorption of moisture, may fairly be regarded as the consequence of faults present in the bushings at the time of manufacture. The logical inference is that if initially bushings are supplied free from flaws, deterioration will not take place under the conditions of service and stress in which these samples operated.

These old bushings were installed after proving tests which we now know to have been inadequate, and it is not surprising that some contained undetected flaws. It is some satisfaction to learn that at the conclusion of site tests on all the old 132 kV bulk-oil circuit-breaker condenser bushings, those found faulty represented but 4% of the whole. The proportion might well have been greater.

The institution of the power-factor/voltage test in 1935 made it possible to discover all the internal defects which lead to deterioration in service. This marked an epoch. Before 1935 bushing manufacture was an art; after then it became a science. The immediate result of the knowledge gained by using the test was a radical change in the design, the raw materials and the manufacturing process; moreover, for the first time, it became possible to eliminate by rejection bushings which suffered from internal defects.

Now that the power-factor/voltage test is established as a routine, and the interpretation of the test results is well understood, it is unlikely that the faults discovered by the authors in the old bushings will be repeated. Failure in service of modern bushings is therefore likely to be the result only of the absorption of moisture, or the destructive effect of surge over-voltages.

Mr. D. M. Cherry: The main value of the paper is its contribution to the assessment of the life of old insulation. We have come reluctantly to realize that plant has to be kept in use until it is judged unsafe to continue with it. This judgment is particularly difficult with insulation. The authors' work has, no doubt, prevented some busbar faults and also unnecessary replacements through lack of adequate knowledge. It must be remembered that most bushings are so placed that their failure entails a busbar fault.

There is still in service much 33 kV metalclad switchgear with compound-filled busbars and bushings exposed to air, and so far there is no satisfactory method of determining the soundness of bushings in such gear. This suggests that the decision of the supply undertakings some years ago to dispense with test tappings on this class of gear may not appear a wise decision to our successors.

In Section 2.2 there is a disparity of outlook in dealing with discharge at foil ends. A prime cause of failure of a layer is stated to be erosion arising from discharge, but, later, a similar discharge is judged to be harmless. When a bushing which has not yet broken down is checked by a power-factor test, how can one distinguish between a discharge that is eroding locally and a general discharge, i.e. between lethal and non-lethal discharges? It may be that for all new bushings of importance a routine X-ray test would be valuable to show whether there is any serious misplacement of foils which might cause an erosive discharge not detectable by power-factor testing.

The authors make no mention of bushings incorporating graphite foil in place of metal foils. Have they conducted any investigation on such bushings? Further, no mention is made of the effect of compound filling, which is liable to mask the characteristics of a bushing itself under power-factor tests.

Much ingenuity has been put into the design and testing of s.r.b.p. bushings, but why are they still used? In service, the oil-filled barrier bushing has a much better record than s.r.b.p. bushings and also the dielectric can be changed or checked. Furthermore, oil-impregnated paper bushings, which have been less used, have an excellent record.

Mr. W. G. Todd: The statement in Section 4 that 'annual site measurements of capacitance will detect punctured sections' requires qualification. The authors, in fact, gave that qualification at the meeting by stating that these tests will detect punctures in all but those rare cases where they are of the self-healing type. I think the site engineer who might be required to use this testing technique would like to know just how rare this condition is. Since it appears that self-healing punctures were found in one bushing which was dissected, and that others also were found to be similarly affected, this condition may not be so rare as we would like. As such punctures can only occur in bushings which contain the very thin foils, it is necessary that the site engineer should know the thickness of the foils in the bushings under test. Is this information available for the earlier bushings? In the case of bushings with very thin foils, how are test results to be interpreted?

Dr. J. H. Mason: Can the authors confirm that the rate of failure of condenser bushings in service has been reduced since the introduction of annual insulation resistance and capacitance measurements?

The authors' statement that the 'knee' of the loss-angle/voltage curve is a good indication of the onset of discharges should be qualified. The loss due to a few discharges of about 20 pC per half-cycle is equivalent to a loss angle of about 10^{-4} in a 100 pF capacitor.* In condenser bushings consisting of 20 capacitors in series the equivalent change of loss angle will be still smaller. Thus increased loss due to discharges will be apparent only when the frequency of small discharges per half-cycle is quite high, or if large discharges are occurring.

Tests on 69 kV solid compound bushings have shown† that, while the 'knee' of the loss-angle/voltage curve may indicate the inception of severe discharges [cf. Fig. F, curves 3(*a*) and 3(*b*)], it may be due to ionic contamination, and occur below the discharge inception voltage [Fig. F, curves 1(*a*) and 1(*b*)], or there may be no sharp rise in loss angle although considerable discharges are occurring, as in Fig. F, curves 2(*a*) and 2(*b*). In condenser bushings it would be possible for some sections to be short-circuited while others were still discharging, so that the resultant loss-angle/voltage curve would be almost flat. The bushing might then be passed for further service although liable to continued rapid deterioration.

I recommend the authors to use an oscillograph detector with their h.v. Schering bridge, so that the magnitude of individual discharges can be determined, and the origin of the discharges distinguished. Gelez‡ uses a galvanometer detector to determine the total losses and an oscillograph to distinguish the components of losses due to conduction and discharges.

* WHITEHEAD, S.: 'Dielectric Breakdown of Solids' (Clarendon Press, 1951).
† BRUSTLE, H. H., JOHNSON, D. L., and SCHEIDELER, A. L.: *Transactions of the American I.E.E.*, 1955, 74, Part I, p. 387.
‡ GELEZ, J. P.: *Bulletin de la Société Française des Electriciens*, 1957, 7, p. 238.

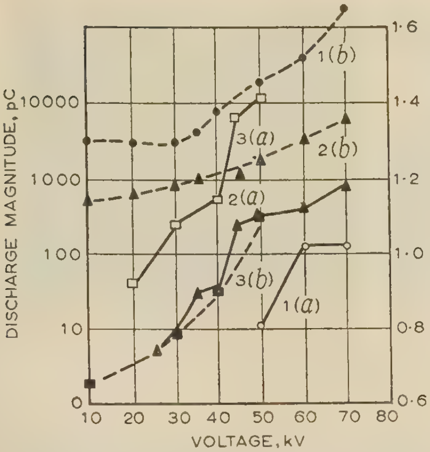

Fig. F.—Discharge and loss-angle/voltage characteristics of 69 kV solid-core bushings, taken from Brustle *et al.*

---- Loss angle.
——— Discharges.

Have the authors made impulse tests on bushings with falling loss characteristics? If several sections were short-circuited by erosion and channel propagation there would be a high probability of breakdown when switching or lightning surges occurred, but if the channels were short-circuited by carbonization no further discharges or deterioration would occur at the service voltage.

Recent tests on simple tubular s.r.b.p. bushings showed that, after 1 900 hours at 6·3 kV (r.m.s.) 50 c/s, discharges between the inner conductor and the surface of the bushing were short-circuited by a semi-conducting layer of deliquescent copper nitrate which was deposited during the test, due to nitric acid formed from nitric oxide generated by the discharge and water vapour in the air. With bushings embedded in bitumen, the absence of water vapour prevents the formation of nitric acid and the discharges continue throughout the test, gradually eroding the insulation, so that the discharge inception voltage falls. In service such embedded bushings have failed after three to four years at 6·3 kV (r.m.s.) 50 c/s.

Mr. F. W. Taylor: That only 4% of the bushings tested were found to be faulty and 3% doubtful is a very good record, but it indicates a mean life much longer than 25 years. We cannot take full advantage of this knowledge or of the improvements in design and processing over this period, because of the standardization of bushings.

Since there are a large number of oil-impregnated paper condenser bushings which have been operating on the Grid system for periods of up to 30 years, I have examined the records of thirty 132 kV bushings which have been in service for 20–25 years and have been returned to the manufacturer for examination and/or overhaul. Of these, 18 had up to 20% lower dielectric losses than when originally dispatched and six were practically unchanged, all having normal characteristics. One had increased losses but was still satisfactory for service when judged by modern standards; one was doubtful but was made satisfactory by re-vacuuming, and four others were doubtful but would not respond to treatment. Therefore, 25 bushings or 83% would fall in Group 1 of the paper.

Incidentally, all these bushings were of the unsealed type and so there would be some interchange of oil from the normal expansion and contraction due to temperature changes. These results, therefore, speak very well for the quality of the oil in the transformers, which were fitted with conservator tanks and whose insulation was also plain paper vacuum-impregnated in transformer oil.

Although some mention is made of discharges at the foil edges, there is no distinction made between the air and the oil ends of the bushing. I should have thought that the difference between the two ends would have been noticeable. Perhaps the authors would comment on this.

Mr. N. Parkman: It is stated in Section 2.2.4 that, in exceptional cases, discharges commence in their bushings at 20 kV (r.m.s.). If we take the authors' figures of 16 sections per bushing, there is a voltage of 1·25 kV across each. However, this calculation is made on the basis of simple condenser bushing theory—that each section has the same capacitance. In fact, the following calculation shows that the capacitance of the faulty section can be only half its original value. For simplicity, the calculation is given for the parallel-plate capacitance case. Fig. G(i) shows a section in which we have a parallel-plate capacitance of

$$\frac{A}{4\pi d/\epsilon} \quad \ldots \ldots \quad \text{(A)}$$

where the symbols have their usual significance. Fig. G(ii) shows the defective section with a capacitance

$$\frac{A}{4\pi}\Big/\left(\frac{d}{\epsilon}+a\right) \quad \ldots \ldots \quad \text{(B)}$$

If we take the authors' values of 3·5 and 0·5 mm for d and a, respectively, and assume the permittivity to be 5 or 7, comparison of expressions (A) and (B) shows that the capacitance

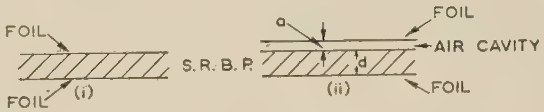

Fig. G

(i) Sound section.
(ii) Faulty section.

of the faulty section is only 0·6 or 0·5 that of a sound section, i.e. the voltage across it becomes about 2·1 or 2·5 kV (r.m.s.). These figures are sufficiently close to the calculated figure of about 3 kV to be accepted as the usual kind of discrepancy for this work.

Such discrepancies arise from the formula which the authors quote, since it is calculated on the assumption that the field in the cavity is uniform. In practice, this is not the case, and it is axiomatic that, if the uniform field is disturbed, it must be made greater at some point.

Secondly, I want to indicate the difference in the information provided by power-factor/voltage measurements and discharge measurements, especially for cavities of large areas. Mr. Cherry wanted to know why discharges sometimes lead to failure and sometimes appear innocuous. The answer lies in whether the large cavities are discharged by a single intense discharge or by many small ones. The former is a very dangerous condition, whereas the latter may be comparatively harmless. Fig. H shows that whether one or many discharges occur will depend on the conductivity of the cavity surfaces. It will be seen that, for surface resistivity of about 10^{14} ohms, many small discharges occur. When the resistivity has decreased to about 10^{7} ohms a smaller number of larger discharges occur, and finally at about 10^{3} ohms the whole surface discharges as a single unit. These transitions can occur spontaneously in service, owing to discharge-induced surface conductivity.*

The discharge detector will indicate whether many small discharges or a few larger discharges are present, whereas the

* E.R.A. Technical Report Ref. L/T364.

(i) (ii) (iii)

Fig. H.—Changes in discharge characteristics with varying surface resistivity.
(i) $\rho_s \simeq 10^{14}$ ohms. (ii) $\rho_s \simeq 10^{7}$ ohms. (iii) $\rho_s \simeq 10^{3}$ ohms.

power-factor/voltage measurement gives only an integrated result for all discharges, large or small. It is possible that, where the applied voltage is low, the discharges will be extinguished by an increase in conductivity of the cavity surfaces. This may well be the case for bushings which are subject to low stresses.

Mr. C. W. Mott: Following field tests on 1 600 bushings, 63 were scrapped and 43 returned to service labelled suspect and scheduled for testing every two to three years.

A total of 1 600 bushings gives approximately 266 circuit-breakers, and assuming one condemned or suspect bushing per circuit-breaker it means that 24% of the circuit-breakers concerned had to be removed from service for major modifications and a further 18% have to be examined every few years during future service life.

Laboratory tests on a random sample of 86 132 kV s.r.b.p. bushings resulted in 20% being scrapped and 28% being returned to service labelled suspect and scheduled for routine testing. More recently 66 132 kV s.r.b.p. bushings were tested at a switchgear manufacturer's works and 13 were found to be suspect. Most probably four will be scrapped and the remainder returned to service subject to routine testing. At another switchgear manufacturer's works 18 132 kV s.r.b.p. bushings were tested and as a result seven were scrapped.

I suggest the electricity supply industry cannot afford this wastage and the technical man-hours required for routine testing of bushings. Results of tests on old bushings can be most confusing, and the power-factor/voltage curves obtained are usually far more complex than those shown by several contributors. Then there are the large number of s.r.b.p. bushings used on lower-voltage equipment which have not been the subject of the same investigations as the 132 kV types. Routine testing of these is impracticable.

Can these bushings be improved, particularly from the point of view of ingress of moisture, so that we can obtain at least 30 years of trouble-free service without the need for costly routine testing in the field? I suggest a higher resin content, subject, of course, to thermal stability not being affected. This would result in power factors somewhat greater than those we are accustomed to expect from modern s.r.b.p. bushings, but I see no objection to this.

Mr. W. A. Cook: To one involved in the manufacture of early Grid bushings with the sparse knowledge then available, our temerity in making 132 kV bushings, for the first time in really large quantities, in 1928–30 is rather horrifying. The 19th-century bridge builders, becoming aware of the dire consequences of metal fatigue, might have thought similarly.

Around 1930, the first two of the three principal desiderata—short-time strength, thermal stability and freedom from ionization—were checked by over-voltage routine and type tests and power-factor/time tests. No accepted tests detected ionization. More foils were known to improve stress control but to increase manufacturing hazards, and a compromise was adopted.

Quite early in the 1930's, with the institution of power-factor/voltage tests, the importance of ionization was realized, as was the value of increasing the number of foils, thereby reducing edge stresses and raising the ionization inception voltage, although the advantage of this is limited by manufacturing problems and the fact that each spiral turn of foil in effect short-circuits a turn of paper. Henceforward, ionization detection by power-factor/voltage or hissing tests and more foils became customary. This was a revolutionary step towards a longer life than the 20–25 years already achieved by the early bushings, which was all that was originally hoped for, and towards eliminating even the odd failure which can count against reliability.

With regard to Mr. Cherry's rather sweeping condemnation of condenser-type bushings, I would only remark that at least one important American manufacturer has recently changed from oil-filled to condenser-type bushings, and that despite growing appreciation in the United States of the importance of ionization.

In view of the possibility of deterioration being triggered off by surges, have the authors any information associating it with the incidence of surges?

Mr. K. H. Stark: The authors conclude that an s.r.b.p. capacitor-type bushing rated at 132 kV reaches a dangerous condition when moisture is absorbed by the paper surface or when the individual capacitor sections are punctured. A convenient instrument for measuring the existence of moisture in paper is the E.R.A. dispersion meter, and I wonder whether the authors have used this instrument.

The authors found that the individual capacitor sections are punctured by discharges eroding through the insulation (Section 2.2.3), and yet it is stated in Section 2.2.4 that severe discharges can exist in a large crack in a bushing without causing any apparent failure or suggesting that the section concerned will fail. I am therefore puzzled as to what constitutes a dangerous discharge.

In the earlier part of the paper it is shown that the power-factor test detected insulation deterioration in bushings examined in the laboratory, but in Section 3.4.2 it is stated that 'the power factor or change of power factor with voltage is not related to the amount of insulation deterioration'. Does this mean that the power factors obtained in the laboratory cannot be applied to site tests?

The authors have made only brief references to the detection of discharges in bushings. They found that the voltage at which

the power factor rises agrees well with the discharge inception voltage as measured with an E.R.A. discharge detector. This is similar to my own experience.

Mr. S. C. Chu (*communicated*): Fig. 2 shows laboratory test results on power-factor/voltage characteristics for five groups of bushings. Was there any special reason why the maximum test voltage for bushings in Group 5 was much lower than those for the other groups, i.e. about 70 kV as against over 100 kV?

In Section 2.2.4 the authors mention the use of the discharge detector, and the results obtained agree with the power-factor rises in bushings in Group 4, but they have not indicated whether such detections have been made on bushings in the other Groups. If so, I should be interested to know how the discharge characteristics differ in the different Groups, with respect to the power factors.

In Fig. 14, I feel that the frequency distribution of capacitance would serve a more useful purpose if expressed as percentages of the original value after installation. It is not clear why the authors omitted a similar curve for the insulation resistance, since they say that its knowledge is essential. A statistical analysis of such data would yield useful information for determining the limits mentioned in Section 3.4.1.

Mr. A. J. Good (*communicated*): Section 2.2.4 refers to the difficulty of explaining certain low discharge inception voltages that have been obtained. I wonder whether the stresses acting over the edges of voids should be considered.

As a first approximation which is mathematically tractable, and apparently permissible, we can assume a very flat semi-elliptical '2-dimensional' cylindrical void of semi-minor axis a and semi-major axis b, situated on a conducting surface which coincides with all the major axes of the elliptical cross-sections. For large values of d/a the stress within the void perpendicular to the conductor surface is the same at any part of the void (actually this would hold even if the ellipse were far from flat so long as d/a was large enough). Taking $a = 0 \cdot 05$ cm, $d = 0 \cdot 35$ cm, $\epsilon = 5$ and $V_i = 1$ kV (r.m.s.), we find V_p, the peak voltage across the gap a at the void centre, to be $640 V_p$. The average stress acting parallel to the insulating surface over a distance a, at the edge of the void, is then given approximately by $(V_p/a)\sqrt{(2a/b)}$. With the above figures, and $b = 0 \cdot 5$ cm, say, we obtain the equivalent of $280 V_p$ acting parallel to an insulating surface of length $0 \cdot 05$ cm. Although this rough estimate still indicates a low value, of about the minimum sparking potential of air alone at s.t.p. (the breakdown voltage at s.t.p. of a pure air-gap of the same thickness of $0 \cdot 05$ cm is $2 \cdot 7 \times 10^3 V_p$), the fact that it is acting parallel to an insulating surface, taken in conjunction with possible impurities and other gases as referred to by the authors, makes it likely that it is high enough to cause discharge inception.

Dr. G. Mole (*communicated*): Substantial progress has been made by the authors in developing non-destructive test methods for bushings of the particular type and vintage concerned. This progress stems from the opportunity to make tests or examinations of a destructive character on a large sample of bushings, no less than half of which show abnormality. In extending the technique to bushings of more modern manufacture, a much greater sample would be required, in order to yield an adequate number of abnormal specimens. The cost of destructive testing or examination would then be considerable and perhaps prohibitive.

With reference to the correlation which has been established between drooping loss angle [group 3(*a*)] and the existence of a punctured section, a little further elucidation would be of value. It appears that the puncture is detectable in this way because of the resistance associated with the carbonized breakdown path. But this method of diagnosis will surely work only if the resistance lies within a certain somewhat restricted range, i.e. if the time-constant constituted by the capacitance of the section together with the resistance of the breakdown path has a value not too widely different from a few milliseconds. The current available for carbonization is, of course, limited, and it may be that the resistances of all carbonized breakdown channels fall, by coincidence, within the optimum range. Can the authors state whether it is possible to get breakdown channels having resistance either above or below the bounds of this range, and so escape detection?

Mr. E. T. Norris (*communicated*): For the higher voltages (66 kV upwards) oil-impregnated paper is practicable and is an altogether superior insulation which has been proved over an operating experience of 30 years. Its characteristics have been described elsewhere.* Could the authors give some information on the service record of this type of bushing of which there have been nearly 1 000 in service in this country from 1929 onwards?

Power factor is often not a reliable criterion for assessing the condition of a bushing. It is not a measure of a single characteristic but the ratio of power to capacitance. In particular, moisture increases the power loss, but also, since its permittivity is high, the capacitance. In one actual case the power loss was increased by 27% by moisture ingress, but the capacitance was increased by 31%, so that the power factor was actually 3·2% better and would quite misleadingly indicate that the bushing was improved instead of being much worse. It is possible to express the power loss in the dimensionless form of power density by means of a constant determined by the geometry of the bushing.

Mr. J. Wainwright (*communicated*): One conclusion of the paper which I find rather surprising is that measurements should be made annually of the insulation resistance and capacitance of 132 kV capacitor-type bushings. Considering that of some 1 600 bushings there have been only, to quote the paper, 'occasional failures' over a period of 25 years, I would be interested to have the authors' views on the necessity for making the test an annual one. With a fault rate of the order of a few bushings per 40 000 bushing-years it is obvious that there is no need for panic measures. On the other hand, the possibility of danger to, or even loss of, human life is something which cannot be measured in economic terms or countenanced on any account.

It is presumed that, in the few failures which have occurred, most of the evidence was destroyed, but it would be very interesting if the authors could give at least some of the more important details.

There must be a large number of similar bushings which have been in service on power transformers where the maximum operating temperature may be in the region of 90° C. Under these conditions erosion would be expected to occur at an increased rate, mainly because of the reduction in the voltage at which foil-edge discharges commence. Since no mention is made of the fact, is it to be inferred that there has been even less trouble with transformer bushings?

From Figs. 13 and 14 of the paper it is possible to estimate that, although about 1 260 loss-angle measurements are given, there is only about half that number of capacitance readings. In both cases the results have a skewed distribution, but tolerable straight lines are obtained when logarithmic probability paper is used. It would be interesting to have the authors' views on the reasons for this.

* COOK, W. A.: 'Outdoor Bushings', *Journal I.E.E.*, 1941, 88, Part II, p. 302.

[The authors' reply to the above discussion will be found overleaf.]

THE AUTHORS' REPLY TO THE ABOVE DISCUSSION

Messrs. J. L. Douglas and **A. W. Stannett** (*in reply*): Several speakers referred to the philosophy of site testing. Both Dr. Forrest and Mr. Mott stated the problem clearly and conclude that it is necessary to try to strike a balance between the cost of testing and overcautious scrapping of equipment and the cost of a failure. Testing methods are subject to change in the light of experience, and we would suggest to Mr. Todd that if, having removed the likelihood of failure due to moisture and normal discharge erosion by the methods recommended, an appreciable number of failures still occur owing to self-healed punctures or for some other reason, the methods would be modified. Such evidence as we have suggests that, if the methods recommended are adopted, the chances of failure due to undetected faults would be very small.

The problem of discharge, its detection and importance, formed the basis of comments by several speakers. From the point of view of the operator it is impracticable and unnecessary to scrap all old bushings which are discharging in service, and our suggested compromise is to reject them when one or two sections puncture (depending on the number of sections present). We would not, of course, recommend accepting from the manufacturer new bushings which discharge at working voltage. As Dr. Mason and Mr. Cook suggest, a bushing containing an eroded path is likely to be slightly weaker to impulses, but we have little information on this point. At the worst, to reject bushings with punctured sections must be an improvement on the old system where they were left in service. Future experience will determine whether the rejection limits need to be amended. It is to be hoped that Mr. Walmsley's work with accelerated discharge tests will assist in understanding the importance of discharges. Mr. Parkman's comment on the change in discharge characteristics with time, and Mr. Walmsley's comment regarding uncertain adhesion between layers and hence the presence of points from which cracks and telescoping may develop in service, do not suggest that discharge tests are likely to be very useful in the field in the near future.

We are grateful for the suggestions made by Messrs. Parkman and Good to explain the low inception voltages observed. Mr. Parkman's assumptions appear to be too sweeping although probably partially correct. The average dielectric constant of the material unaffected by discharge is about 4·2. The high dielectric constants produced by the discharge seem to be confined to a small volume such that taking a whole section thickness the dielectric constant of affected material is about 4·7. From actual measurements of the capacitance of sections containing voids, it was found that the voids reduced the capacitance as predicted, but in no case was the decrease more than 10% and usually it was less, and certainly not 100% as Mr. Parkman suggests. Section inception voltages and dielectric constants calculated from the section capacitances were used in our unsuccessful efforts to explain the low bushing inception voltage, and this method takes into account the increased voltage due to the void capacitance.

With regard to specific points, Mr. Ashley presented some results which cannot usefully be discussed without further data. We have observed effects like those shown in Fig. B which were found to be caused by poor electrical contacts on the bushing assembly, e.g. a film of paint between the arcing ring and the helmet to which it was bolted. Fig. C shows that damp bushings cannot always be dried. We agree with this conclusion and feel that it is an undesirable practice because cracks may form during the drying-out process. We have found no general trend of the increase of power factor with age, as shown for location 2 in Fig. D, and would suggest that the bushings from location 1 do not show such a trend either. The difference between locations 1 and 2 could be explained by the presence of damp oil at one of the sites at some time during their service life.

In reply to Mr. Cherry, we are not aware of having tested any bushings with graphite foils. In our experience, bushings which are compound-filled have slightly higher power factors than the oil-filled types, but otherwise the same analytical methods are applicable.

With reference to Dr. Mason's query about fault rates, no bushing which has been tested *in situ* during the past five years has since failed in service. It is postulated that a rising power factor due to discharges in some sections might mask the fall due to punctured sections, and Messrs. Norris and Mole also raised points in a similar vein, but errors such as these are not likely to be made if the capacitance is taken into account.

In reply to Mr. Taylor, there was no significant difference in discharge erosion between the air and oil ends of bushings.

Mr. Stark suggests the use of the dispersion meter for damp bushings. Our experiments show that this instrument will not always respond to moisture, particularly where it affects only the outer bushing skin. With this exception and also excepting drooping power factors, dispersion correlates with power factor and both are affected by the circuit-breaker components. For this reason the power-factor (and dispersion) level measured on a complete circuit-breaker are meaningless so far as the condenser bushing is concerned.

Mr. Chu was puzzled by the fact that in Fig. 2 some characteristics were plotted for voltages up to 50 kV only. There is no significance in this. We do not agree with his suggestion that a distribution of insulation-resistance results would be informative. Such a curve does not give much information, and, in any case, this aspect of the subject has been reported previously. Fig. 14 could not be plotted as suggested, since the original capacitances are not known.

Discharge tests were carried out on representative bushings of all groups, but the inception voltages were high except for the Group 4 bushings. Since the results did not appear to aid the investigations, they were not made on a routine basis.

We assure Dr. Mole that the puncture paths do have resistances in the range mentioned, but the range is not too restricted, as shown in Fig. 5.

Mr. Wainwright assumes that higher temperatures would adversely affect the discharge characteristics of a bushing, but the paper by Brustle, Johnson and Scheideler, referred to by Dr. Mason, suggests that this assumption may be incorrect. We have tested only a few transformer bushings, but do not suspect that their condition is very different from oil-circuit-breaker bushings. Annual measurements of insulation resistance have been made for many years, and are largely responsible for the low number of bushing failures due to moisture ingress. With the development of the insulation-resistance/capacitance test set, the additional test takes only a minute or two longer to make. This annual measurement of capacitance may be erring slightly on the cautious side, but we do not think that these simple tests can be construed as 'panic measures'. Probability paper is not a very reliable method of determining the normality of a distribution, since a fair straight line can be obtained with very skew distributions. Generally, a histogram gives a clearer idea of the distribution.

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A CONJUGATE-IMPEDANCE NETWORK ANALYSER OPERATING AT 50 c/s

By W. CASSON, Member, and A. W. HALES, Associate Member.

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SUMMARY

The paper outlines the design and performance of an economical conjugate-impedance type of network analyser operating at 50 c/s which was developed by the Central Electricity Authority for use by the various Electricity Boards and Divisions. The means and compromises adopted to achieve the specified operational performance are discussed. The development of the analyser involved, amongst other things, the design and development of a composite equipment for the measurement of voltage, current, real power, reactive power, phase angles and—when used in conjunction with a calibration source—the calibration of the shunt and series impedance elements used in the analyser. A comparison between the data obtained by measurements from the network analyser and rigorous calculations for four types of problem is given.

(1) INTRODUCTION

The Central Electricity Authority and Electricity Boards of this country have long recognized the advantages of using network analysers to assist their system design engineers in studies relative to the planning of system modifications and extensions. In the former years the d.c. type of network analyser, which is relatively cheap to construct, was generally considered to be adequate for this purpose. In the relatively few cases where design information could not be obtained from this type of equipment, use was made of a.c. network-analyser facilities available at the works of leading electrical equipment manufacturers.

Owing to the increasing complexity of supply systems and the number of problems that are requiring to be studied on a.c. network analysers, there has arisen a demand from system design engineers to have their own a.c. network analyser equipment. A joint working party of the Central Electricity Authority and the Electricity Boards was asked to investigate the possibility of obtaining an a.c. network analyser which would provide adequate operational facilities at reasonable cost. The working party concluded that to obtain conventional a.c. equipment in this country based on existing designs would necessarily be expensive and make for difficulty in complying with the desired specification, so they decided to develop an equipment which would meet their specific requirements.

The working party had to decide whether to provide facilities for both transient-stability and steady-state studies. Having regard to the circumstances in which the analyser would be employed and to facilitate standardization of the equipment, it was decided that it should be applicable to steady-state a.c. power-system studies and synchronous-machine transient-stability studies effected by the step-by-step method. Furthermore, it was decided that the following conditions should, if compatible with economic production, be fulfilled:

(*a*) The equipment should require the absolute minimum of accommodation space.
(*b*) It should initially be of a completely 'universal' design, readily extensible, and lend itself in individual circumstances to the addition of an economic 'geographical' extension.
(*c*) It should require the absolute minimum of maintenance and be capable of a consistent high level of operational performance without frequent specialist maintenance and checking.
(*d*) The main network instrumentation should be such that it could be calibrated and checked from time to time having recourse to the facilities normally existing in meter test laboratories of the Divisions of the Authority or Area Boards.
(*e*) The analyser should utilize for its network excitation a supply derived directly from the 50 c/s mains, thus obviating the necessity of providing separate oscillator and ancillary amplifier equipment.
(*f*) The components used in the functional units should be readily available and present no replacement difficulties during the anticipated life of the equipment; also they should not possess critical tolerance values.
(*g*) The mean overall accuracy of the analyser when applied to system problems for which rigorously calculated solutions were available should be better than $\pm 2\%$ at nominal frequency. The measuring equipment, when considered as a separate entity, should have an overall accuracy of better than $\pm 1\%$.

It was on the basis of the above conditions that work commenced on the design and development of a prototype (Mark I) equipment; the Mark II design, now in quantity production, incorporates numerous refinements, and the paper is based on this design.

(2) DESIGN

(2.1) General Considerations

Initially the direct-impedance type of analyser operating at mains frequency was considered, but was found to be unattractive both from the economic and component supply viewpoints and was therefore not pursued to an advanced stage. After reviewing the potentialities of the alternative types of analyser and the relevant operational and economic considerations, it was decided that, in order to meet as many of the conditions detailed in the Introduction as possible, the conjugate-impedance type should be adopted, i.e. an analyser in which the inductive and capacitance elements are transposed with respect to the actual system being represented. Accordingly, work began on the design and development of a prototype equipment using the mains 50 c/s as base frequency. The representation of system elements on both types of a.c. analyser is shown in Fig. 1. An analyser using the conjugate-impedance principle operating at 120 c/s (obtained by doubling a 60 c/s mains frequency) has been described elsewhere,[2] but it differs in certain respects from the one described here. Apart from inevitable differences in physical construction, the chief points of difference in the present design are as follows:

(*a*) The use of feedback amplifiers in the generator units is eliminated.
(*b*) The metering equipment is designed to enable all measurements to be made with one main instrument, which replaces, functionally, three separate instruments.
(*c*) The base current and power values employed are half those previously used, while the base frequency of 50 c/s has rendered mains-frequency conversion equipment unnecessary.

To secure the minimum cost it was decided to employ uncalibrated network impedance elements, and since these would for the most part consist of resistors and capacitors, the use of readily available commercial wide-tolerance components could be permitted. The conjugate-impedance principle and 50 c/s operation having been adopted, the remaining base operating

Written contributions on papers published without being read at meetings are invited for consideration with a view to publication.
Mr. Casson and Mr. Hales are with the Central Electricity Generating Board.

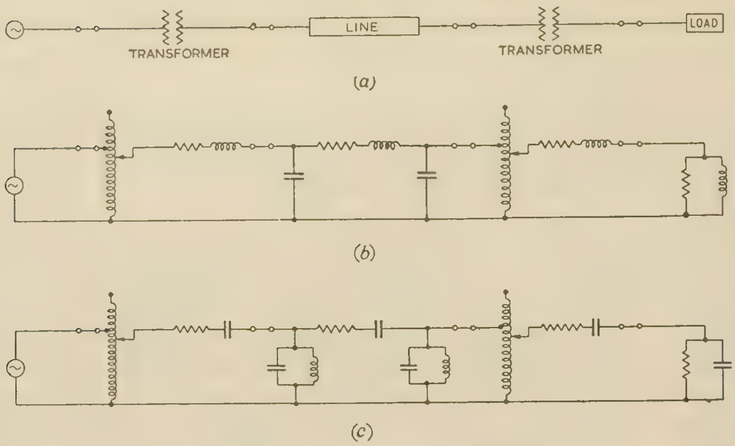

Fig. 1.—System representation.

(a) Actual system.
(b) Representation on conventional-impedance analyser.
(c) Representation on conjugate-impedance analyser.

quantities were determined by the operating voltage of the magslip transmitters used in the generator and impedance calibration units and the range of commercially available capacitors, particularly in respect of capacitance and physical dimensions [the tubular paper capacitors commonly available have capacitance ratios of either 1 : 2 or 1 : 5 and nominal tolerances (depending upon capacitance) in the range $\pm$20–25%, which, for a small increase in cost, can be obtained with closer tolerances, say $\pm$10%].

The magslip transmitters in the generator units operate at 50 volts r.m.s., and this was adopted as a convenient base voltage for the analyser. To secure the maximum financial economy it was desired to utilize the minimum sizes of capacitor, and to this end a high-impedance base was desirable: however, too high an impedance would have resulted at the design stage in the adoption of an undesirably low current base, and initially it was not considered prudent from considerations of metering equipment and auto-transformer design to use a base current of less than 5 mA. Consequent upon the decision to use this base current the resulting base values were

Voltage	50 volts.
Current	5 mA.
Impedance	10 kilohms ($C = 0{\cdot}318\,3\mu$F).
Power	250 mVA.
Admittance	100 microhmos.
Frequency	50 c/s.

The use of a base power of 250 mVA enabled auto-transformers of adequate performance to be designed without resort to the use of core-loss compensating equipment. However, in the light of the established performance of the measuring equipment and the more recent availability of improved transformer-core material, there is no valid reason why a lower current and power base should not be used, thus reducing the size of capacitors still further. However, the fact that the finite residual output of the metering-current equipment-channel amplifier is predominantly of mains frequency makes its effect upon metering errors one of importance, and any reduction in power and current bases from 250 mVA and 5 mA respectively would require additional precautions to minimize its significance. No strong case for a change in the above base quantities has emerged in the light of operational experience on the prototype (Mark I) equipment.

(2.2) Extension to include a Geographical Feature

Although the prototype equipment was originally designed to be completely universal in application, it lends itself to easy integration with a geographical network layout where desired. Such a feature would be inherently compact and inexpensive, since the series and shunt impedance elements of the system would ordinarily consist of pre-calibrated resistor(s) and capacitor(s). A network of this type would provide suitable facilities for carrying out routine basic studies and could be supplemented by the addition of auto-transformer and shunt-susceptance units according to the nature of the system under study, the range of problem which it was desired to solve and the ultimate accuracy required. It is commonly required to provide only for load-flow and short-circuit studies on systems whose characteristics do not justify the inclusion of tap-changing and shunt-susceptance facilities.

(3) DESCRIPTION OF EQUIPMENT

The layout of a typical medium-capacity equipment is shown in Fig. 2, from which it will be seen that the analyser consists of a number of functional units centred on a common equipment rack; those provided initially are the generator, load impedance, line-impedance, auto-transformer and shunt-susceptance units, although in practice the numbers of these units incorporated in a particular analyser would be chosen according to the operational needs of the area in which it was located. The common-equipment rack contains the 50 c/s mains supply unit, the main network

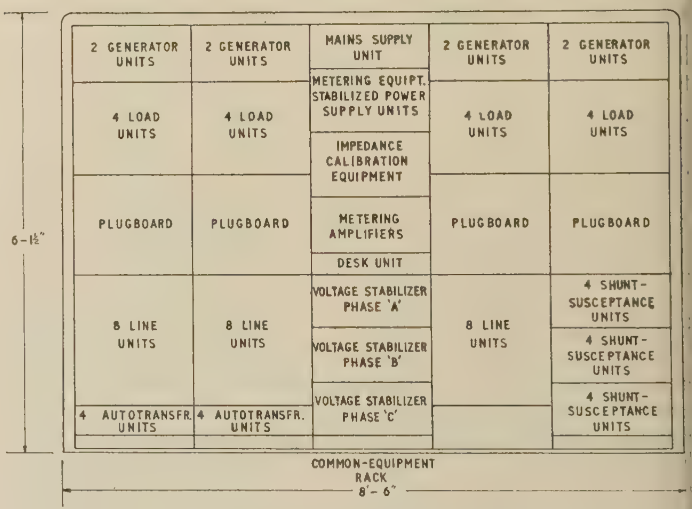

Fig. 2.—Frontal layout of typical production equipment.

measuring-equipment amplifiers, with their stabilized power-supply equipment and associated desk-instrument units, and the impedance calibration equipment. Automatic mains-voltage regulating equipment is an optional feature whose provision depends upon the local supply conditions.

(3.1) Generator Units

The generator units, which provide sources of e.m.f. independently variable in both phase and voltage, consist essentially of a magslip-transmitter/variable-transformer combination, the former being used to control phase-shift and the latter to control voltage. The 3-phase, 50 c/s supply to the magslip transmitter stator is at 50 volts r.m.s. Provision is made for the indication of output voltage by a voltmeter calibrated 0–1·5 per unit volts and fitted with a red index pointer. The drive to the magslip transmitter shaft is effected through a slow-motion dial assembly having a reduction of 50 : 1 and a vernier

scale, thus giving an angular resolution of 0·1°. An on/off switch is provided in the output (rotor) circuit for disconnecting the unit from the network. The unit when set to give a terminal voltage of 1·00 per unit has an output impedance of approximately $0·007 + j0·002$ per unit.

(3.2) Load-Impedance Units

The arrangement of a load-impedance unit for representing a lagging-power-factor load is essentially a parallel *RC* network, both resistance and capacitance being variable and supplied from the secondary of an auto-transformer, the latter having a range of voltage deviation of ±0·20 per unit in 0·01 per unit increments. The resistive branch of the load impedance consists of two potentiometers connected in parallel, together with additional padding resistors, both potentiometers having self-contained on/off switches. The range of real power available is approximately 0–2·0 per unit. The reactive branch of the impedance consists of three ranges of switched capacitors, the range of reactive power availably being approximately 0–1·20 per unit. Details of the auto-transformers, which are of the same basic design as those used in the auto-transformer units, are included in Section 3.4. Both resistance and capacitance ranges have their setting positions identifiable, thus enabling settings frequently employed to be recorded and retained for future use.

Voltmeters of the same basic pattern as those used in the generator units monitor the voltage across the load impedance and so facilitate the maintenance of constant power consumption. Four load-impedance units are accommodated on each panel.

(3.3) Line-Impedance Units

The line-impedance units, of which there are eight per panel, comprise two ranges of resistance and four of capacitance, the resistive and reactive networks being connected in series. The resistance ranges comprise a potentiometer and a group of switched resistors covering approximately the ranges 0–0·06 and 0–0·50 per unit impedance respectively, the total coverage thus being approximately 0–0·56 per unit impedance. The normal operational capacitance range is approximately 0–1·0 per unit impedance and has a minimum resolution of approximately 0·006 per unit impedance. The lowest reactance setting normally obtainable is about 0·03 per unit impedance (corresponding to approximately 10 μF), although this can be reduced further if considered economically justifiable by the inclusion of an additional capacitor without modification to the switching circuits. All the resistive and reactive components in these units, with the exception of the potentiometers, have a tolerance of ±10%, the capacitors being of the tubular paper type. The nominal capacitance values and ranges were originally selected having regard to the preferred values currently available; thus the complement of capacitors for each individual unit is as follows:

Range I:	$9 \times 1·000\,\mu F = 9·000\,\mu F$
Range II:	$9 \times 0·100\,\mu F = 0·900\,\mu F$
Range III:	$9 \times 0·010\,\mu F = 0·090\,\mu F$
Range IV:	$5 \times 0·002\,\mu F = 0·010\,\mu F$
Total ..	$10·000\,\mu F$

When it is necessary to have a nominal π-configuration for a transmission line, provision is made on the termination panel section of the plugboard for the insertion of shunt-susceptance units. The switch-position settings appropriate to frequently-used impedances can be recorded, and after an accumulation of setting data has been obtained recourse to the use of the impedance calibration equipment would be relatively infrequent within the area in which the analyser was normally employed.

(3.4) Auto-Transformer Units

This type of unit comprises an auto-transformer together with associated ratio-change switching facilities, four such units being accommodated on each panel. To represent the equivalent series impedance of a transformer a unit is used in conjunction with a line-impedance unit. The winding arrangement adopted enables a voltage deviation of ±0·20 per unit voltage to be obtained in 0·01 per unit increments. This is achieved by having thee main sections of winding, i.e. one 1·0 per unit section, one 4 × 0·04 per unit section and one 4 × 0·01 per unit section, ratio adjustment being effected by variation in tap selection and winding sense of each sectionalized winding.

The transformers are wound on Mumetal cores and have a total loss when supplying nominal full load (i.e. 250 mVA at unity power factor from the high-voltage winding with a ratio of 1 : 1·20) of 0·015 per unit power, i.e. 3·75 mW of which approximately 0·001 5 per unit (0·375 mW) is attributable to the winding copper loss. The magnetizing component of the no-load admittance is compensated, at unity ratio, by the inclusion of a 4 500 pF capacitor in parallel with the main 1·0 per unit voltage winding. The equivalent leakage impedance of the transformers is $0·001\,7 + j0·000\,46$ per unit impedance and $0·003\,5 + j0·000\,9$ per unit impedance for the maximum boost and buck conditions respectively.

(3.5) Shunt-Susceptance Units

A parallel *LC* circuit forms the basis of a shunt-susceptance unit, the capacitance ranges being designed to give a near-continuous variation in capacitance, while the inductance, having one tapping, provides two values of 430 and 172 henrys respectively. The inductance, which is wound on a Ferroxcube core, has a Q-factor on either tapping of not less than 25, two such units being mounted in a hermetically-sealed container. The desired capacitance range is provided by two banks of moulded mica-type capacitors (tolerance ±10%) which are selected by 18-position switches. The total capacitance range is nominally from zero to 39 600 nF and when used with the above inductances gives an overall range of susceptance of 0·03–0·19 per unit. Typical calibration curves for the ranges provided by the two inductances quoted are shown in Fig. 3.

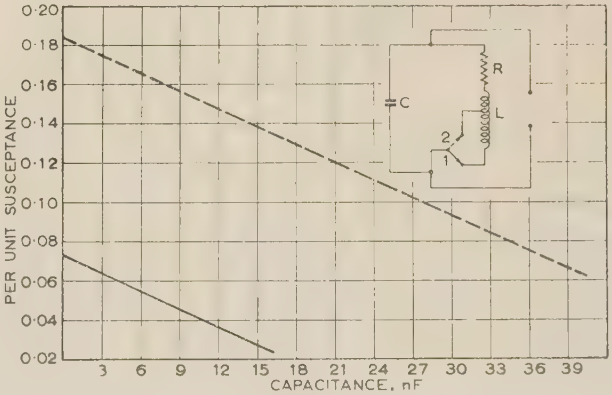

Fig. 3.—Calibration curves for shunt-susceptance unit.

The normal operating range traverses the region between $Q = 25$ and 8, the latter being assumed to be the minimum value consistent with adequate accuracy of representation. The effective changes in line-to-neutral voltage on nominal setting values are depicted in Fig. 4 for settings of 0·18 and 0·10 per unit susceptance respectively. Throughout the normal opera-

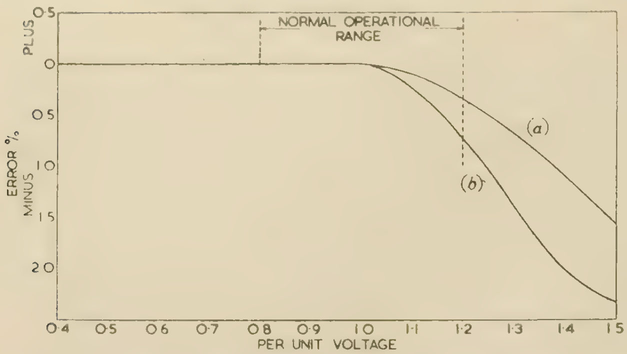

Fig. 4.—Variation of susceptance-unit setting with applied voltage.

Nominal setting at 1·0 *per-unit voltage:*
(*a*) 0·18 per unit.
(*b*) 0·10 per unit.

tional range under the most pessimistic conditions the deviation from nominal setting is less than 1%. An extension of the range of susceptance can be effected by a suitable choice of inductance and/or capacitance.

(4) MAIN NETWORK MEASURING EQUIPMENT

This equipment was designed to perform the functions of network impedance calibration and measurement in the analyser network. It can be used when dissociated from the analyser in low-power mains-frequency applications where low instrument burdens are essential. It consists in essence of two separate amplifier channels, one each for the current and voltage circuits respectively, together with their associated stabilized power-supply units and a single indicating instrument. When used in conjunction with the impedance calibration equipment it enables measurements of the following quantities to be made.

Voltage
Current
Real power
Reactive power
Relative voltage phase angle } In conjunction with variable-phase
Relative current phase angle } impedance calibration source.
Resistance }
Reactance } In conjunction with variable-output-voltage
Conductance } impedance calibration source.
Susceptance }

The ranges of voltage and current measurement provided are show in Table 1.

Table 1

VOLTAGE AND CURRENT RANGES

Quantity	Range	Per unit value for f.s.d.	Scale multiplier	Actual reading for f.s.d.	
				volts	mA
Voltage	1	0·250	0·20	12·50	—
	2	0·625	0·50	31·25	—
	3	1·250	1·00	62·50	—
	4	2·500	2·00	125·00	—
Current	1	0·250	0·20	—	1·250
	2	0·625	0·50	—	3·125
	3	1·250	1·00	—	6·250
	4	2·500	2·00	—	12·500
	5	6·250	5·00	—	31·250

(4.1) Design

A functional schematic of the metering system is shown in Fig. 5, from which it is seen that the current-channel amplifier is divided into two sections—a 2-stage pre-amplifier unit and a 2-stage output amplifier. A feedback loop applies negative

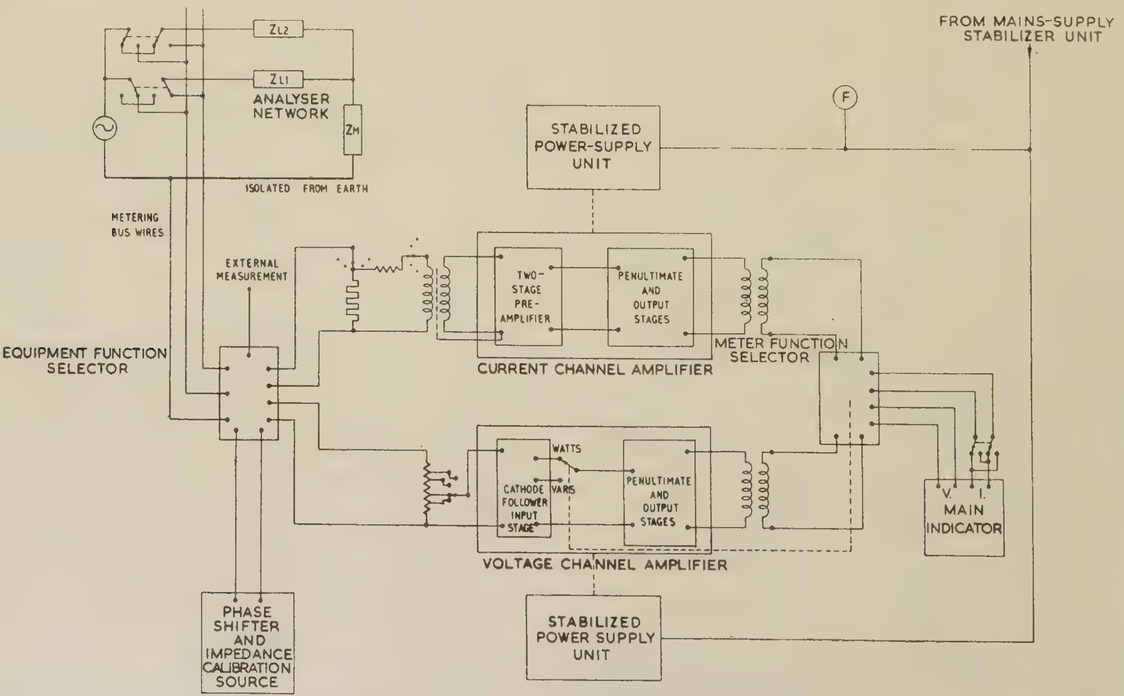

Fig. 5.—Functional schematic of metering system.

Equipment-function selector
1. Series-impedance calibration.
2. Shunt-impedance calibration.
3. Network measurement.
4. Voltage phase-angle.
5. Current phase-angle.
6. External measurement.

Meter-function selector
1. Voltage.
2. Current.
3. Real power.
4. Reactive power.

voltage feedback over the two pre-amplifier stages (48 dB), while a second loop provides negative current feedback (55 dB) over the penultimate and output stages respectively.

The pre-amplifier of the current channel derives its input from the voltage drop produced across a precision shunt ($\pm 0 \cdot 1\%$ tolerance) which is inserted in the analyser network at the point of measurement. The insertion effect of the largest ohmic value of shunt on the network is equivalent to an additional $0 \cdot 001$ per unit impedance in the network. The input transformer of the channel, which is of unity ratio, is designed in respect of primary inductance (15 henrys) so as to ensure that a negligible shunting effect on the metering shunt is achieved. It is also electrostatically and electromagnetically screened. The current ranges detailed in Table 1 are obtained by the insertion of differing values of shunt in the network. The maximum output current of the channel (100 mA r.m.s.) is obtained with an input voltage of $12 \cdot 5$ mV r.m.s. The input-transformer secondary provides the input for the first stage of the pre-amplifier—an EF86 high-gain pentode characterized by an inherent low noise level. It will be observed that a switched resistor is connected in series with the primary of the input transformer, the function of which is to balance the effect of primary-to-secondary capacitance current which flows in the primary winding of the transformer. An equivalent circuit of the input circuit of the channel is shown in Fig. 6. In practice, the balance resistance

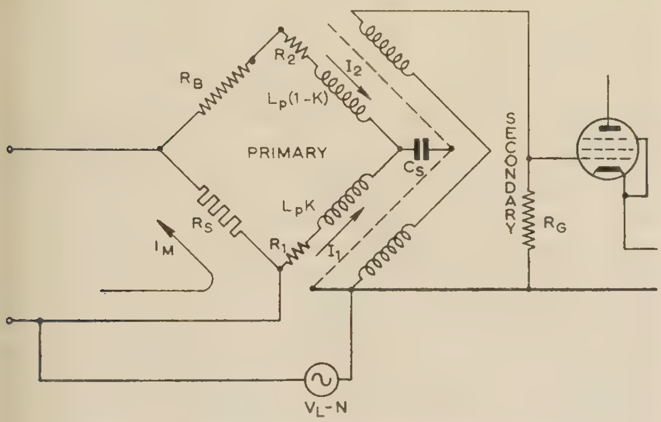

Fig. 6.—Current-channel-amplifier input circuit.

to give minimum secondary induced e.m.f. for a particular shunt value is found empirically. The degree of unbalance introduced by range changing is insufficient to necessitate rebalance of the network on each range, two separate balance conditions being adequate. Any unbalance in this input circuit results in current measurements becoming dependent upon the sense in which the measuring shunt is inserted in the network and the phase of the network current with respect to the line-to-neutral voltage of the analyser at the point of measurement. The second stage of the pre-amplifier and the penultimate stage of the output amplifier are *RC*-coupled EF91 pentodes, the latter being followed by a single-ended 6BW6 beam-tetrode output stage. The negative current feedback applied over the output amplifier stages results in an output impedance of 900 kilohms. Final adjustment of output is effected by adjustment of the feedback loop gain over the output stages. The total harmonic distortion introduced by the channel when delivering rated output is not greater than $0 \cdot 1\%$, while the total hum and noise level is not greater than $0 \cdot 1\%$ of the current corresponding to full-scale deflection, i.e. 100 mA r.m.s. The linearity between input and output is within $0 \cdot 2\%$ of the output for full-scale deflection in the range 50–100% output current.

The voltage-channel amplifier derives its input from a resistance voltage-divider network inserted between the line and neutral of the analyser network. The insertion effect of the divider on the analyser network at $1 \cdot 0$ per unit voltage corresponds to a current drain of $0 \cdot 002$ per unit, i.e. the channel has an input impedance of 5 megohms. The input voltage, which is $12 \cdot 5$ volts r.m.s. for maximum current output (100 mA r.m.s.), is fed initially to a 6BR7 cathode-follower stage from which two alternative outputs differing in phase by 90° are available, the selection of output being dependent upon whether measurement of real or reactive power is required. The output and penultimate stages of the voltage-amplifier channel are of identical design to those of the current-amplifier channel, and the performance of the channel in respect of linearity is the same as for the current channel, while the total hum and noise level is considerably less. The connection of the main indicating instrument to the two amplifier channels is performed automatically by a function-selector switch which determines the quantity measured, i.e. voltage, current, real or reactive power. The selector switch, in addition to determining the function, inserts appropriate padding resistors in the output circuits of the amplifier channels so that they always work into a constant impedance (100 ohms). The main indicating instrument is a precision-grade dynamometer movement having a current for full-scale deflection of 100 mA r.m.s. It has two scales, namely a square-law scale for current and voltage measurements, coloured black and calibrated 0–$1 \cdot 25$ per unit, and a linear scale for real and reactive power measurements, coloured red and marked 0–$1 \cdot 50$ per unit. The impedance of its moving system (current) is $11 \cdot 41 + j0 \cdot 021$ ohms, the corresponding value for the fixed (voltage) system being $10 \cdot 22 + j8 \cdot 0$ ohms. The use of negative current feedback in the output stages with a consequential high output impedance has two advantages, namely the use of high-stability close-tolerance components in the load-impedance circuit of each channel is avoided, and the inherent errors in measurement introduced by the finite value of mutual inductance between the fixed and moving systems of the dynamometer movement are considerably reduced, the maximum value of the mutual inductance in the instrument employed being $0 \cdot 125$ mH. With an output impedance of 800 kilohms and this degree of extraneous coupling, negligible errors due to this cause are obtained. The operating current for full-scale deflection of 100 mA r.m.s. was chosen to give a reasonable compromise between considerations of output-stage design and an adequate torque/weight ratio for the moving system of the dynamometer movement.

Each amplifier channel is supplied from a separate stabilized power-supply unit of conventional cathode-follower design, this arrangement minimizing any possible interaction between the two channels by virtue of a common supply-source impedance. To facilitate the rapid determination of valve operating conditions and fault location, self-contained monitoring facilities are incorporated. A mains-frequency meter provides a continuous indication of mains-frequency deviation to operating personnel. Ordinarily frequency deviations in excess of $\pm 0 \cdot 2$ c/s rarely obtain.

The overall accuracy of the metering equipment, being determined as it is by the combined performance of separate components each possessing errors, some of which are variable, makes a quoted figure of overall accuracy rather ambiguous. Besides specifying the conditions of measurement, the input-transformer ratio and phase-angle errors, the shunt error, the amplifier linearity error and the error due to the finite resolution of the amplifier gain adjustments should be all quoted for specified conditions of measurement and the combined effect evaluated. However, the more practical alternative of carrying out calibra-

tion checks using precision-grade instruments with known correction factors in conjunction with standard load resistors and capacitors has shown that an accuracy of measurement of the order of $\pm 0\cdot 5\%$ of instrument full-scale deflection is attained over a period of several months, during which frequent mechanical handling of the equipment has taken place, this being entirely adequate for the intended application of the measuring equipment. The performance of the metering system in relation to mains-frequency variations has been evaluated using a power generator source whose output voltage is characterized by a low harmonic content (approximately $0\cdot 3\%$ total). The variations in real- and reactive-power measurement with frequency are illustrated in Fig. 7 for specified load conditions.

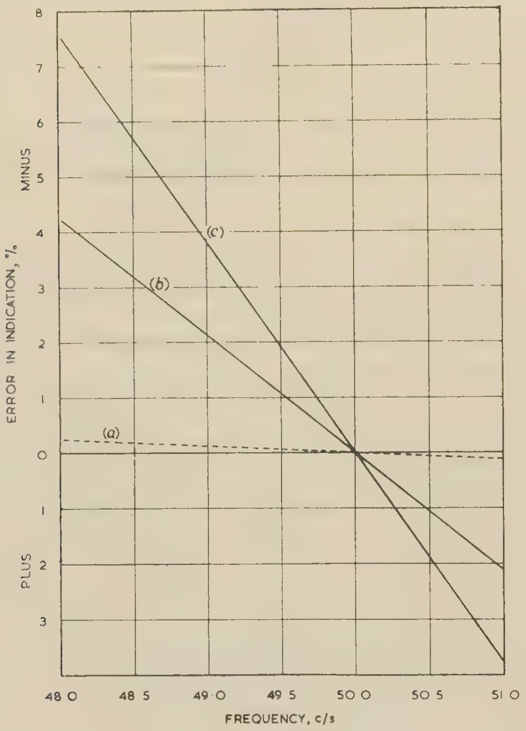

Fig. 7.—Variation of power-metering error with frequency.

(a) 1·0 per unit real power; 0·70 power factor, leading.
(b) 1·0 per unit reactive power; zero power factor, leading.
(c) 1·0 per unit reactive power; 0·70 power factor, leading.

It will be seen that with normal frequency deviations reactive-power measurements are affected by $\pm 0\cdot 25\%$, while real power measurements remain virtually unaffected.

(4.2) Calibration Procedure

The main indicating instrument can, when desired, be given a calibration check as a separate unit in a meter laboratory in conjunction with a d.c. potentiometer equipment. Since the instrument is of the air-cored dynamometer type and operates at 50 c/s it can be used as a transfer device, the d.c. errors being assumed to be applicable to a.c. operation.

With the equipment inserted in a test network operating at unity power factor and selected to measure reactive power, the relative phase of the output currents of the two amplifier channels is adjusted until a zero reading is obtained on the main instrument.

This procedure is repeated with the equipment inserted in a test network operating at zero power factor lagging or leading and selected to measure real power. The relative phase of the output currents of the two amplifier channels is adjusted (by another phase-shift adjustment) until a zero reading is obtained on the main instrument.

With a milliammeter connected in series with the main instrument voltage (moving) coil, the outputs of the in-phase and quadrature networks of the first stage of the voltage-channel amplifier are adjusted so that the output current is the same when the equipment is selected to measure either real or reactive power.

With the milliammeter removed the overall gain of the voltage-amplifier channel is adjusted using a precision-grade voltmeter with known correction factors as a standard.

With the equipment selected for current measurement and inserted in a network which has a load consisting of a 1·0 per unit non-inductive resistor ($\pm 0\cdot 1\%$ tolerance) and with a supply voltage of 1·0 per unit, the overall gain of the current-channel amplifier is adjusted so as to give 1·0 per unit current indication. If the equipment is now selected for real-power measurement the real-power indication will not normally exceed $\pm 0\cdot 2\%$ of its nominal value. The various current ranges can be individually checked using different standard non-inductive load resistors, while the reactive-power range calibration can be checked using appropriate low-loss capacitors. Experience indicates that this latter check is not commonly necessary.

(4.3) Impedance-Calibration Unit

This is designed for the calibration of the shunt and series impedance elements which constitute the analyser network. The shunt elements are calibrated while 1·0 per unit voltage is maintained from the calibration-source series-impedance elements being calibrated, while 1·0 per unit current circulates through them. The real- and reactive-power dissipation in the unknown impedance which it is desired to calibrate is measured by the main metering equipment, the actual per unit values of the impedance components being obtained directly from a consideration of the amplifier range multipliers. A source of variable e.m.f. is derived from a magslip transmitter with a 3-phase stator winding: when it is fed on two phases only, adjustment of output voltage is effected by angular rotation of the rotor; when fed on all three phases it is used in conjunction with the main metering equipment (operating as a null indicating device) for the measurement of relative voltage and current phase-angles. To ensure ease of operation and high accuracy, the magslip transmitter rotor is driven through a slow-motion drive assembly having a reduction ratio of 50 : 1, its vernier scale being mounted on an adjustable cursor; this enables an angular resolution of 0·10° to be obtained. To avoid frequent functional selection of the metering equipment, a separate voltmeter and milliammeter are incorporated for the measurement of calibration voltage and current respectively, these being checked initially against the master instrument to ensure correspondence of readings.

On this unit is also mounted a selector switch which determines the application of the main measuring equipment. The six facilities available are

(a) Shunt-impedance calibration
(b) Series-impedance calibration
} Using variable-voltage constant-phase impedance-calibration source.

(c) Network measurement

(d) Relative voltage phase-angles
(e) Relative current phase-angles
} Using variable-phase constant-voltage impedance-calibration source.

(f) External measurement

For facility (f) three plugs are provided which give access to the neutral and line terminals of the voltage- and current-channel amplifiers respectively. The use of a constant-voltage constant-current source has been considered, but experience has indicated

that it is by no means an essential facility, since impedances can, in practice, be rapidly calibrated by relatively inexperienced operating personnel. A practical aid to the speeding up of the impedance-calibration procedure is the formation of a library of unit dial settings for frequently encountered impedances.

(5) MAINS POWER SUPPLY

(5.1) Mains Power-Supply Unit

The function of this unit is to provide a 3-phase 3-wire 50-volt supply for the generator and impedance-calibration-unit magslip transmitters, and it comprises three single-phase 150 VA transformers connected in star on both primary and secondary windings; the normal range of input voltage is 400–430 volts, 3-phase, and the transformer rating provides sufficient capacity for ten generator units plus one impedance-calibration unit. A mains isolating switch and protective fuses are also incorporated.

(5.2) Mains-Voltage Stabilizing Equipment

Although operational experience on the prototype equipment has demonstrated that mains-voltage stabilizing equipment is by no means an essential adjunct for the satisfactory operation of the analyser, it is recognized that there are locations where mains-voltage variations would make the provision of stabilizing equipment a necessity. To meet this contingency, automatic stabilizing equipment can be incorporated to reduce the amplitude of mains-voltage variations to a level where they are no longer of operational importance. In this particular type of analyser—where enhancement of harmonics already present in the mains supply must be avoided, since they appear in magnified form in the current waveforms in the network—it is important that the type of automatic regulator employed, while maintaining its output voltage between close limits, should not enhance the harmonic content of the mains supply. To fulfil these conditions a regulator using a servo-operated variable-ratio transformer to inject a series buck or boost voltage into the mains supply line is utilized. In essence, the variable-ratio transformer is driven by a small 2-phase servo-motor, the output voltage of the regulator being impressed on a bridge network which, when the regulator output voltage is at its nominal value, gives zero output. The bridge network becomes unbalanced following a departure from nominal-output-voltage conditions and provides an error signal for a feedback restoration loop. Velocity feedback is incorporated in the system to enhance the loop stability and minimize the effects of friction. The r.m.s. output voltage is stabilized, its long-term stability being such that the output voltage does not change typically by more than one or two volts per annum (in 230 volts). The speed of response for small voltage deviations, i.e. ± 3 volts, is approximately $0\cdot25$ sec ($12\cdot5$ cycles of the supply frequency), and for deviations in excess of this a response of $0\cdot15$ volt/sec is obtained. The regulator will maintain the r.m.s. output voltage to within $\pm 0\cdot25\%$, it being specified to operate within these tolerance limits under the following conditions:

Input voltage	$-17\cdot5$ to $+8\cdot75\%$.
Load current	0–9 amp.
Load power factor ..	0–1 (lagging or leading).
Frequency..	45–65 c/s.
Temperature variation ..	From normal ambient up to 20° C.

Since these units are basically of single-phase design, three are required for each analyser installation.

(6) PLUGBOARD ARRANGEMENT

The functional elements of the analyser are mounted in enclosed racks, each rack being associated with a plugboard assembly situated in a standard position. The plugboard normally

(a) Serves as a central termination panel for the functional units within the rack.
(b) Accommodates the circuit selector switches used to insert the main metering equipment in the functional units within the rack.
(c) Enables functional units to be assembled to represent a network which it is desired to study.
(d) Permits of direct connection to the impedance calibration equipment located in the common-equipment rack and the main analyser neutral busbar.

The rear assembly of the plugboard serves to extend the 3-phase 50-volt distribution wiring, main metering and impedance-calibration bus wires throughout the whole length of the equipment. This arrangement permits of the future extension of an analyser without recourse to modification of internal interconnection wiring. Designation strips are provided for each row of selector switches and plugs to ensure adequate coded identification.

(7) PERFORMANCE OF PROTOTYPE (MARK I) EQUIPMENT

(7.1) Performance Tests

In order to gauge the operational performance and accuracy of the Mark I equipment, it was used to study many representative types of problem whose solutions were accurately known. Four classes of typical problem employed are load flow, short-circuit, voltage regulation involving the use of series capacitance and synchronous-machine transient stability. The results obtained from random solutions on the analyser together with rigorously calculated values are shown in the following Sections.

(7.2) Two-Line Load-Flow Study

The system configuration together with impedance constants for this problem is shown in Fig. 8, while the corresponding vector diagram (not to scale) of the system is shown in Fig. 9.

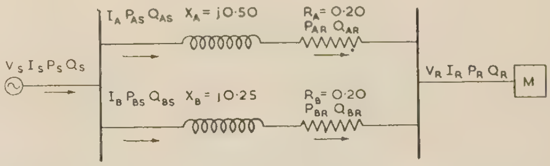

Fig. 8.—System diagram for 2-line load-flow problem.
All quantities are per-unit values.

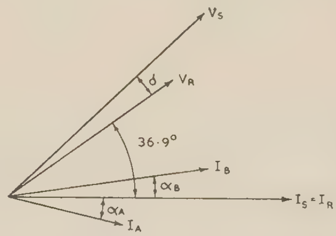

Fig. 9.—Vector diagram for system shown in Fig. 8.

The comparison of measured and calculated values is shown in Table 2.

The mean arithmetic error obtained corrected to instrument full-scale deflection is approximately $0\cdot27\%$, while the corresponding algebraic error is $+0\cdot17\%$. The errors in angular measurement are not shown, since the phase-shifter scale marking

Table 2

TWO-LINE LOAD FLOW STUDY: COMPARATIVE DATA

Quantity	Calculated values	Measured values	Error
	per unit	per unit	(% f.s.d.)
V_s	1·24	1·236	−0·3
I_s	1·25	1·260	+0·8
P_s	1·17	1·162	−0·6
Q_s	1·02	1·030	+0·8
I_a	0·47	0·475	+0·8
P_{as}	0·36	0·360	0·0
Q_{as}	0·45	0·450	0·0
I_b	0·79	0·805	+1·3
P_{bs}	0·81	0·810	0·0
Q_{bs}	0·56	0·560	0·0
P_{ar}	0·32	0·320	0·0
Q_{ar}	0·35	0·350	0·0
P_{br}	0·68	0·680	0·0
Q_{br}	0·40	0·400	0·0
V_r	1·00	1·000	0·0
P_r	1·00	1·000	0·0
Q_r	0·75	0·750	0.0
αA	10·6°	10·0°	—
αB	6·3°	6·0°	—
δ	4·15°	4·0°	—

on the prototype equipment did not permit of resolution to better than 0·25°. The values obtained were, however, within ±0·25°.

(7.3) Network Fault Study

The network chosen for this study[3] (Fig. 10) contains reactive elements only, and is particularly valuable since it involves the

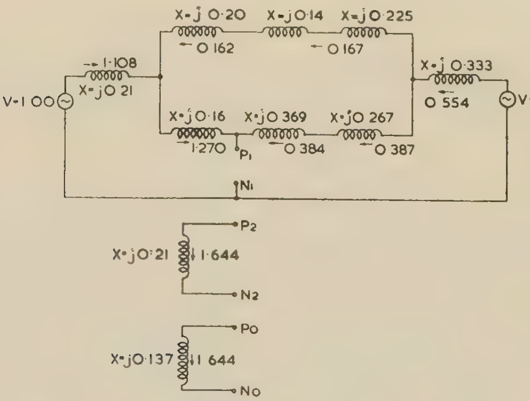

Fig. 10.—System diagram for network fault study.

All quantities are per-unit values.

reduction of negative- and zero-sequence network impedances for its solution. A single line-to-earth fault was placed upon the network at point P_i, the quantity measured being the total fault current. The measured value of 1·644 per unit current when compared with the calculated value of 1·637 gives an error of +0·35% f.s.d.

(7.4) Voltage Regulation Study

This problem, which has been used elsewhere[4] to obtain an assessment of a.c. network-analyser performance, relates to the application of series-capacitance line compensation to a system, it being required to determine the real power transmitted at various load power factors at specified constant sending- and receiving-end voltage conditions.

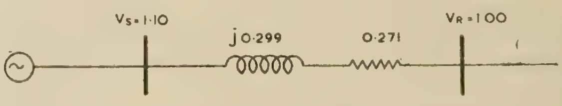

Fig. 11.—System diagram for series-capacitor application problem.

All quantities are per-unit values.

The system is shown in Fig. 11, while a comparison of measured and calculated values is shown in Table 3.

The mean arithmetical error obtained was +0·85%, and the corresponding mean algebraic error +0·52%, i.e. 0·25% f.s.d.

(7.5) Transient-Stability Study

A transient-stability study[5] was carried out on the system shown in Fig. 12 employing the step-by-step method of solution. The comparison between calculated and measured angle/time data for each increment of time is shown in graphical form in Fig. 13 for two fault conditions.

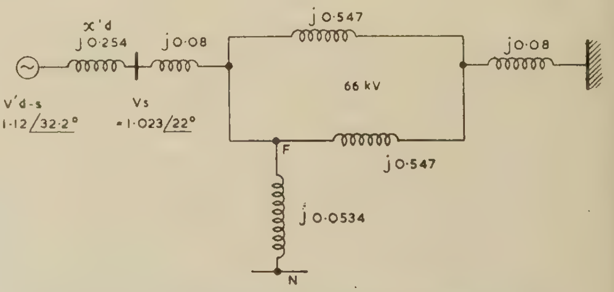

Fig. 12.—System diagram for transient-stability study.

All quantities are per-unit values.

$H = 3$; $K = \dfrac{180 \times 60}{3 \times 1} = 3\,600$.

$V_R = 0{\cdot}95$; $P_R = 50$ MW (0·833 p.u.); $Q_R = 0$; $I_R = 460$ amp 0·877 p.u.).

Measured values obtained on analyser: $V'_{d-s} = 1{\cdot}123$; $V_R = 0{\cdot}95$; $\phi_s = 32°$.

Table 3

VOLTAGE REGULATION STUDY: COMPARATIVE DATA

Line compensation	Load transmitted														
	Power factor* = 0·80			Power factor* = 0·90			Power factor* = 0·95			Power factor* = 0·98			Power factor* = 1·00		
	Calculated	Measured	Error	Calculated	Measured	Error	Calculated	Measured	Error	Calculated	Measured	Error	Calculated	Measured	Error
%	MW	MW	%	MW	MW	%	MW	MW	%	MW	MW	%	MW	MW	%
0	20·2	20·5	+1·5	24·0	24·3	+1·2	26·8	27·0	+0·7	29·4	29·5	+0·3	35·0	34·4	−1·7
20	22·0	22·1	+0·5	25·6	26·0	+1·6	28·4	28·3	−0·4	30·8	30·8	0·0	35·4	35·5	+0·3
45	24·6	25·0	+1·6	28·0	27·9	−0·3	30·2	30·4	+0·7	32·4	32·5	+0·3	36·2	36·0	−0·6
60	27·6	27·8	+0·7	30·4	30·9	+1·6	32·4	32·1	−1·0	33·8	34·2	+1·2	36·6	36·7	+0·3
85	31·4	31·6	+0·6	33·4	33·7	+0·9	34·4	34·4	0·0	35·2	35·9	+2·0	36·8	37·2	+1·1

* The load power factor was lagging in each case.

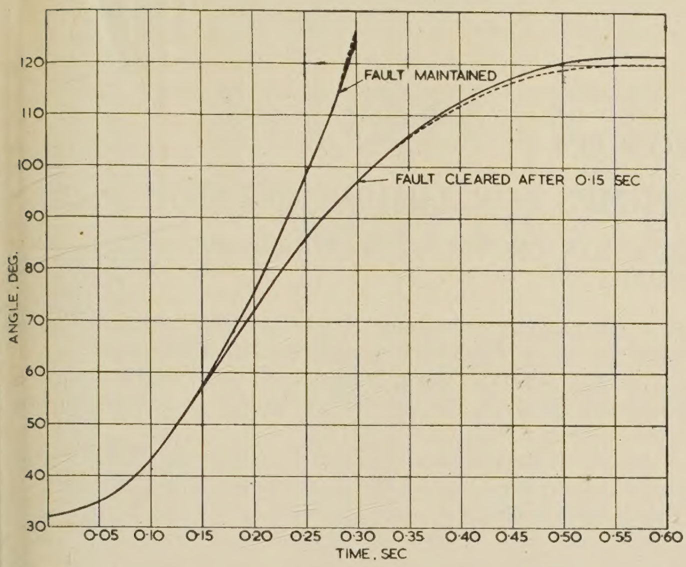

Fig. 13.—Comparative curves for transient-stability study.
—— Calculated.
---- Measured on analyser.

(7.6) Analysis of Performance Results

An analysis of the many studies undertaken on the prototype equipment has indicated the existence of certain small errors. These are occasioned by

(*a*) The presence of stray line-to-neutral capacitance in the line-impedance units.

(*b*) The finite, though small, residual output of the current channel amplifier.

(*c*) Slight unbalance of the input bridge network of the current-channel amplifier.

The line-unit stray capacitance amounts to some 50 pF, while the interconnection leads between the units and plugboard contribute upwards of another 20 pF. Reductions in both these values have been effected on the Mark II equipment by careful attention to mechanical design and layout. The effect of increasing the size of an installation is not critical so far as stray capacitance is concerned, since the line-impedance units are located immediately adjacent to the plugboard terminations.

Experience indicates that the effect of the variable capacitance contributed by the frontal interconnection leads is not a significant factor. The error introduced by the finite residual output of the current-amplifier channel is variable and depends upon both the magnitude and relative phase angle (with respect to line-to-neutral voltage) of the current being measured. Its effect is more important in this equipment owing to the fact that the measured current and the predominant component of the residual output are of the same frequency.

The error introduced by unbalance of the input circuit of the current-channel amplifier is again dependent upon both phase and magnitude of the output current with respect to the line-to-neutral voltage. Owing to the complex quantitative effect of the above factors it is difficult to assess their combined significance for all conditions, but it is considered that under the most pessimistic conditions a maximum error of approximately 0·003 per unit current obtains.

Experience in operating the Mark I equipment has shown that, even with personnel having a limited amount of operational experience, it is possible to achieve considerable speed and accuracy in operation. The Mark II production equipments, with their attendant refinements, further enhance this performance.

(8) ECONOMIC CONSIDERATIONS

A comparative assessment of the costs of an a.c. network analyser of the type outlined in the paper relative to (*a*) a conventional d.c. network analyser, and (*b*) a conventional direct-impedance 1 kc/s network analyser, has been made on the basis of similar conditions of manufacture. It indicates that for similar operational capacities the cost of (*a*) is approximately half that of the 50 c/s equipment while (*b*) is approximately three times as costly. Thus the manifold advantages attendant upon having available an a.c. rather than a d.c. equipment can be obtained for a modest increase in capital outlay.

(9) CONCLUSIONS

The form of network analyser evolved, while fulfilling in large measure the conditions initially postulated, has resulted in there becoming available an analyser which is capable both of being economically produced under ordinary commercial conditions and of providing and maintaining a high degree of operational performance and accuracy. The development of the analyser has consequentially resulted in the availability of a multi-purpose single-frequency precision measuring equipment which can be used as a self-contained piece of laboratory apparatus. The latter, although intentionally operating at relatively low power levels, where conventional instruments of comparable burden and accuracy are not available, lends itself to extension for measurements at higher power levels where negligible insertion burdens are important.

To further extend the application of the analyser, the design and development of other types of unit, e.g. sequence network-coupling transformers and series susceptance units, is proceeding, these being readily integrated with the overall design adopted.

(10) ACKNOWLEDGMENTS

The authors wish to thank the members of the joint working party of the supply industry, who carried out the investigations resulting in the decision to proceed with the development of the analyser, and also for their valuable suggestions during its development. The assistance afforded by Mr. E. B. Powell and his staff, of the London Division, Technical Department, during the development phase is also recognized. They also wish to thank the Chief Engineer of the Central Electricity Generating Board for permission to publish the information contained in the paper.

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(5) Reference 3, p. 460.

DISCUSSION ON 'CONDUCTION AND INDUCTION PUMPS FOR LIQUID METALS'*

SOUTH MIDLAND SUPPLY AND UTILIZATION GROUP AT BIRMINGHAM, 11TH NOVEMBER, 1957

Dr. W. G. Thompson: The ingenuity of the designs of liquid-metal pumps and the development of their theory as described in the paper are very impressive. In the early days of the war my colleagues and I were interested in the development of liquid-mercury pumps to establish a mercury jet in mercury-arc rectifiers which could be used as an ignition device after the manner of the ignitron. The idea was successful electrically, but the jet *in vacuo* was susceptible to the slightest mechanical shock. In the course of this work a d.c. pump consisting of a flat spiral between two vitreous-enamelled steel blocks was developed, the magnetic field being between the blocks, the current flowing radially outwards while the mercury was fed through a central hole and driven around the spiral. Accurate bedding of all surfaces was essential to prevent leakage, and it occurs to me that similar problems might be encountered in the fitting of the helix in the induction pumps described by the author. The d.c. pump was subject to the low-voltage limitations to which the author refers.

Attention was also turned to a pump of the type shown in Fig. 1, but working with alternating current. Again manufacturing difficulties had to be overcome, but it also suffered from a fundamental difficulty, namely that the performance of a conduction pump largely depends upon the ratio of the resistance of the tube to the resistivity of the liquid, which, in our case, was 100 microhm-cm compared with 97 for mercury. Various other factors were encountered, including the concentration of the current, the low boiling point of the liquid under vacuum and the formation of films on the inside of the tube. So far as could be observed, the pumps functioned perfectly under atmospheric pressure but showed idiosyncrasies under vacuum.

One pump which has not been mentioned and which is of Italian origin converts the rotational energy imparted to mercury by a rotating field to pressure energy. The effect was poor at 50 c/s but quite good at 150 c/s. At that time 150 c/s was considered an undesirable complication, but to-day these higher frequencies are in common use and a pump of this type may well merit further consideration.

Although the liquid-metal pumps have relatively low efficiencies, it would appear that for many metals the loss may be a useful contribution to maintaining the liquid in its required state.

Mr. R. A. York: The author states that the flux density in conduction pumps is lower than is normal in motors. What is the flux density in the liquid metal in these cases?

All the pumps described have one stage; if greater pressure is required with certain liquids, is the usual thing to change the type of the pump or to have double-stage pumping?

What voltage is used for the windings of the larger induction pumps?

* BLAKE, L. R.: Paper No. 2111 U, July, 1956 (see 104 A, p. 49).

Is the performance of any of these pumps affected by the fact that they are in some cases pumping radioactive liquids?

Mr. F. C. Barford: Apart from nuclear projects, has consideration been given to the use of these pumps in normal industry, such as with molten tin?

Mr. G. S. Buckingham: The average engineer is rather startled by the idea of a flow of 10 000 gal/min of liquid sodium at 400° C, and would naturally be interested to learn more details of the precautions taken to ensure the safety of operatives. What happens if there is a leak or a pipe bursts when full of this extremely dangerous material? On the other hand, what happens if there is a failure of the supply of electricity and the sodium solidifies in the pipes?

Mr. H. J. Gibson: Can the speed of pumping be controlled, and, if so, how is this effected? Presumably for nuclear-power purposes a constant speed is not objectionable, and perhaps the heat loss can be regulated in other ways by the supplementary heat-extraction pumps, but it appears that a varying speed would almost certainly be needed for industrial applications.

Mr. H. M. Fricke also contributed to the discussion.

Dr. L. R. Blake (*in reply*): Further to Dr. Thompson's most interesting comments, all electromagnetic pumps must be pressurized to prevent cavitation at the lowest pressure region of the pump, usually at the pump inlet at the smallest duct cross-section. Cavitation occurs at an absolute pressure corresponding to the vapour pressure of the liquid, so that operation under vacuum would cause difficulties.

In reply to Mr. York, the flux density is 10–15 kG in conduction pumps and 1–2 kG in induction pumps. For reliability of insulation, the winding voltage of large induction pumps is best kept low, at 250–500 volts. The radioactivity of the liquid does not affect performance, except indirectly by restricting the choice of electrical insulation. Two pumps are sometimes used in series for increased pressure, but usually the design or type of pump is changed.

In connection with the point raised by Mr. Barford, consideration has been given to the use of electromagnetic pumps in the die-casting industry and in chlorine plants for pumping mercury; a form of electromagnetic pump has also been used to stir liquid steel in arc-furnaces. However, the greatest interest has been shown for nuclear projects, particularly for pumps of large size. Consequently, in reply to Mr. Buckingham, the sodium hazard in large pump systems is often increased, owing to its induced radioactivity, so the highest standards of safety are essential. General details of safety precautions using sodium are given in Reference 1 and its supplement.

Pump flow is normally controlled by variation of the supply voltage, although, as Mr. Gibson points out, constant flow is often acceptable in nuclear-power applications.

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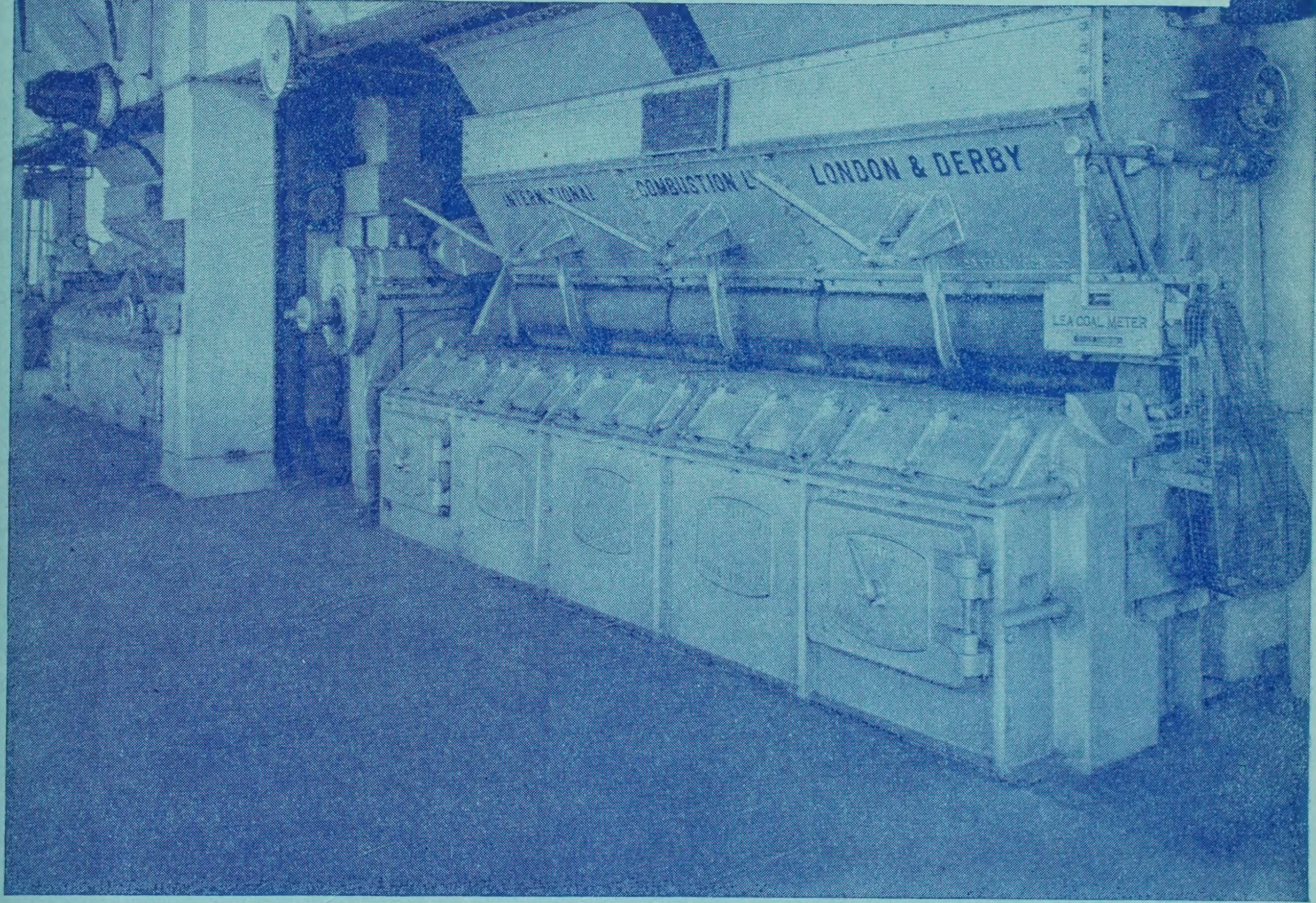

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PROCEEDINGS OF THE INSTITUTION OF ELECTRICAL ENGINEERS

Part A. POWER ENGINEERING, JUNE 1958

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